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Elastic Theory in Sheet Metal Forming Problems

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Introduction

THE MAIN OBJECT OF THIS PAPER is to help bridge the gap that exists between the scientific knowledge of materials and the practical application of that knowledge to the production technique of sheet metal forming. During the past year the Production Research Group of Lockheed's engineering department has given special attention to this important problem and has worked closely with the production departments in an effort to put sheet metal forming on a scientific basis. The following discussion is based largely on the work of the Production Research Group, as reported in various references and in papers yet to be published. Mr. William Schroeder and Mr. G. A. Brewer of this group have been particularly helpful to the author in the preparation and editing of the technical material. Because of the scope of the present paper, detailed discussion and analysis of new developments cannot be undertaken; however, such information will be made available as soon as possible in the form of individual papers by those directly responsible for the work.

The use of the term "elastic" in the title may appear to be somewhat inaccurate as applied to forming problems, which, of course, involve plastic deformation. It is felt, however, that the principles of elasticity should be considered to include the entire range of stress-strain relationships, as it is impossible to divide the behavior of metals into two completely independent parts. As used here, then, the term "elastic theory" includes all theories dealing with change of shape resulting from the application of stress.

Symbols used in this paper follow aircraft terminology as given in reference 10 (ANC-5).

Part 1
Important Forming Properties of Metals

Before an analytic attack on forming problems can proceed it is necessary to obtain certain physical data for the metals involved. Our investigations have shown that the ordinary stress-strain diagram, suitably modified and extended, is the key to almost all forming problems. This means that there is really no mysterious or unique property which is a measure of formability; it is only necessary to extend our knowledge along lines already established.

An ordinary stress-strain diagram for 24ST Alclad material is shown in Fig. 1. Note that for curve "A" the strain scale is such as to cover the entire range to failure. In forming work it is important to have the complete stress-strain diagram for the tension range. Note also that the curve is extended some distance into the compression range. This information is often useful in dealing with buckling problems.

Maximum Elongation

The total strain just before the instant of failure represents the maximum elongation of the material—that is, the amount it can be stretched before breaking. For typical aluminum alloy materials (24ST Alclad) this value is in the neighborhood of 19 per cent (Fig. 1). The standard measurement of elongation is made over a 2-in. gauge length in a tensile coupon held between grips which are about 5 in. apart. In forming work,

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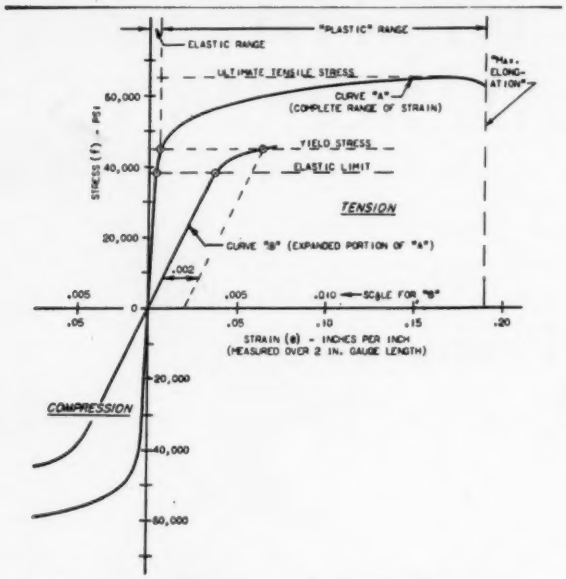

FIG. 1. Typical stress-strain diagram (24ST Alclad).

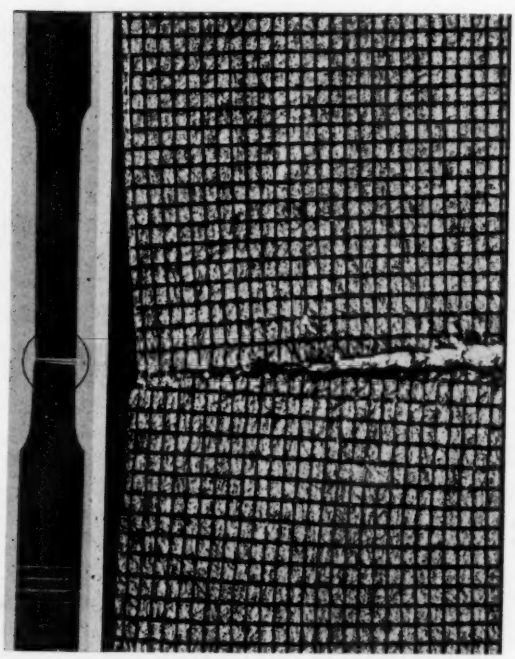

FIG. 2. Typical test specimen for measuring local strain using 100-line per inch grid (24ST Alclad).

however, the length being stretched may vary from a small value (such as $^1/_4$ in. in the case of a sharp bend) to a large value (such as several feet in the case of a sheet being formed by stretching), and it was therefore necessary to determine the maximum elongations obtainable under these widely varying conditions.

The first studies were made as described in reference 1, by photoprinting a fine gridwork on a tensile specimen and measuring the actual strains occurring in

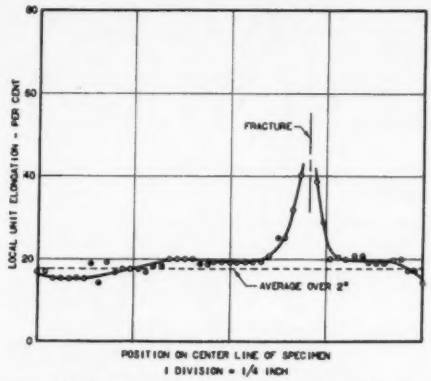

FIG. 3. Distribution of elongation, measured over 0.02-in. gauge lengths, in a standard tensile test specimen of 24ST Alclad. Reproduced from reference 1.

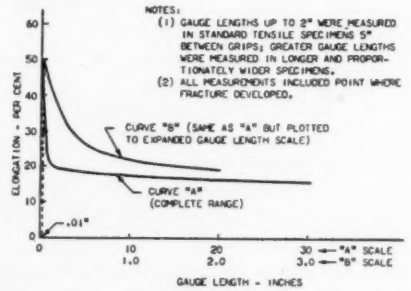
NOTES:
(1) GAUGE LENGTHS UP TO 2" WERE MEASURED IN STANDARD TENSILE SPECIMENS 5" BETWEEN GRIPS; GREATER GAUGE LENGTHS WERE MEASURED IN LONGER AND PROPORTIONATELY WIDER SPECIMENS.
(2) ALL MEASUREMENTS INCLUDED POINT WHERE FRACTURE DEVELOPED.
ELONGATION - PER CENT
CURVE "B" (SAME AS "A" BUT PLOTTED TO EXPANDED GAUGE LENGTH SCALE)
CURVE "A" (COMPLETE RANGE)
.01"
"A" SCALE
"B" SCALE
GAUGE LENGTH - INCHES

FIG. 4. Variation of maximum elongation obtainable with gauge lengths over which it is measured (24ST Alclad).

short gauge lengths from an enlarged photograph or by means of a cathetometer (measuring microscope). A typical specimen is shown in Fig. 2. The results of such measurements were plotted against position along the specimen, showing how the strain varied locally just as fracture occurred. Fig. 3 shows such a curve. To obtain the total elongation over any particular length of the material, the average height of the curve over this length must be determined. By selecting various lengths (always including the point where fracture is about to develop) and performing this process, data are obtained for the type of curve shown in Fig. 4.

The work thus far described has been limited to exploration of elongations locally existing in a standard tensile coupon of fixed length between grips (see note on Fig. 4). Evidence tends to indicate that if the distance between the points at which the material is gripped were decreased to correspond to the gauge length being explored the maximum elongations obtainable would be at least as great. For example, in a sharp bend in sheet metal the elongation can take place only over, and adjacent to, the bend allowance, so that in effect the gripping points are within $^1/_4$ to $^1/_2$ in. of each other. Experiments show that high elongations are obtainable in the outer fibers of such bends comparable with those indicated by measurements over $^1/_4$- or $^1/_2$-in. gauge lengths in a standard tensile coupon gripped in the usual way.

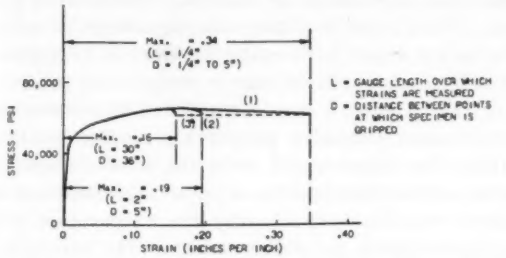

FIG. 5. Strain diagrams based on various gauge lengths (24ST Alclad).

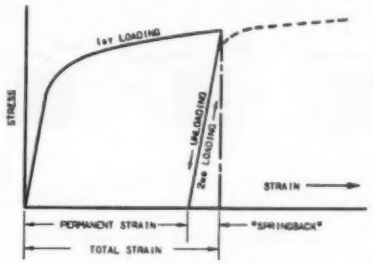

FIG. 6. Stress-strain diagram for successive loadings.

If we attempt to proceed in the other direction, however—that is, toward long distances of stretching—the tensile coupon of standard length can no longer be used, and studies have to be made on longer pieces of material. Questions regarding the effect of the width of the strip, etc., then begin to arise, but enough experimental work has been done to establish approximate limits as indicated in Fig. 4. It is believed probable that the region of high local elongation adjacent to the point of failure (Fig. 3) retains about the same width and height regardless of the length of the specimen between grips, so that as the latter increases the local rise becomes less and less important as a percentage of the whole. Thus, the average elongation obtainable over the entire specimen tends to decrease to a level equivalent to that existing just outside the region of local rise. The relation between the latter region and the rest of the strain distribution curve appears to be a matter involving the strain hardening properties of the material.[1]

The curve of local strain distribution may be considered as a most important characteristic, but it is not in itself an independent property of a material. Actually, the same general information could be conveyed by expanding the stress-strain diagram to apply to various gauge lengths. Although this is not a convenient form of presentation, it helps to clarify the problem. Fig. 5 has therefore been prepared to indicate the probable effect of gauge length on the stress-strain diagram. The maximum elongations correspond with those shown in Fig. 4. The distances between points of gripping the specimens are recorded in each case.

Reference 1 gives further information on the nature of local strain. Additional work has been done to determine the local strain curves for various materials. It is hoped that such information will eventually be supplied by the producer of the material in the form of curves such as are given in Figs. 4 and 5. Vendors of steel now recognize the importance of local strain indirectly by means of the bend test specification.

PERMANENT DEFORMATION

The usual stress-strain diagram represents a one-way loading to failure. Since forming operations involve going only part of the way to failure, it is of special interest to know the behavior of metals after the load has been released and, in some cases, after it has been applied again. This subject is well covered in reference 11. The most important points can be summarized as follows:

(a) During unloading, the stress-strain diagram is substantially elastic in nature—i.e., becomes a straight line having the same slope as the elastic portion of the basic diagram.

(b) Upon reloading, the new stress-strain diagram tends to follow the unloading line until it reaches the original basic curve, after which it follows that curve.

These points are illustrated in Fig. 6, which shows conditions for two successive loadings. Further unloadings and reloadings would exhibit the same general tendencies.

Fig. 6 shows that if the loading is carried well into the plastic range the permanent strain will be large in comparison with the "spring-back" or elastic recovery. On the other hand, any loading condition that does not stress the material beyond the proportional limit (i.e., which does not go beyond the initial straight portion of the S-S diagram) will have a complete elastic recovery. The two extremes may be illustrated by (1) a soft metal (such as lead) which has a low yield stress and large elongation, and (2) a hard metal with a high yield stress and low elongation (such as a steel spring). The high-strength aluminum alloys, when formed after heat-treatment, represent an intermediate condition in which both permanent set and spring-back occur.

COMBINED STRESSES

The foregoing discussion is concerned entirely with simple tension. In many forming problems combined stresses are present; in fact, it will be shown later that combined stresses are essential for certain types of forming (see also reference 17, appendix B). Theories of failure under combined stresses are adequately covered by many references[2, 3, 4, 8, 17] and represent a promising research field. Although a scientific treatment of this subject is beyond the scope of this paper, a few generalized ideas will do much to explain the behavior of metals under various forming conditions.

Instead of regarding simple tension as the basic condition, let us consider two pure states of stress, which

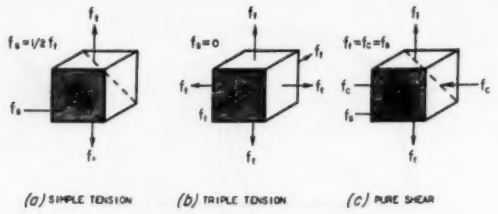

FIG. 7. Fundamental stress conditions.

will be designated as "triple tension"* and "pure shear." These limiting conditions are illustrated diagrammatically in Fig. 7, which also shows the simple tension condition (a) with its well-known maximum shear components along the 45-deg. diagonal plane.

In the so-called triple-tension condition (Fig. 7b) each particle of the material is subjected to uniform tensile stress in all directions. Fig. 7c shows combined equal tension and compression in two dimensions. This represents a state of pure shear in the two planes which are inclined at an angle of 45 deg. to the directions of tension and compression loading. (One such plane is shown; the other is normal to it.) The axial stresses normal to these shear planes are zero. Fig. 7c could be further modified to show compression on all four vertical faces, thus producing pure shear in any plane inclined at 45 deg. to the direction of tension loading.

The stress conditions on any plane through the loaded specimen may be determined analytically or graphically (by Mohr's circle). In the case of triple tension (Fig. 7b) there is no shear stress (i.e., no sliding tendency) *on any plane;* the entire action tends to pull the elements apart. If the directions of loading were reversed, every particle would be under pure compressive stress in all directions and again there would be no shear stress present.

In the case of pure shear (Fig. 7c) the stress condition varies from pure tension normal to the horizontal plane (as illustrated), through pure shear on the diagonal plane to pure compression normal to the vertical plane. All intermediate planes are subjected to a combination of normal and shear stresses.

The state of stress at various angles is illustrated by the polar diagrams of Fig. 8, in which the normal and shear stresses are plotted for each of the three previously described conditions.

TRIPLE TENSION

The case of triple tension is of special interest in forming work because of its negative significance. It represents, practically, a complete absence of formability. Permanent deformation of the specimen under these conditions would require a permanent increase in volume, just as a decrease in volume would be required for deformation by uniform (hydrostatic) pressure. For all practical purposes the change in volume of a metal under hydrostatic pressure is negligible—actually there is a slight elastic compression (reference 3, p. 11). Hence, it can be concluded that under uniform tension (negative pressure) a similar condition exists. The metal would resist the tensile stresses up to the point of failure without appreciable deformation. Failure would have to occur by direct separation of the metal, involving no sliding action. The strength so attained would be higher than that usually obtained in a simple tension test and is usually designated as the cohesive strength. The determination of this fundamental property of a material has recently been the subject of many interesting experiments (references 5 and 9, for instance), but for the purposes of this paper it is only necessary to recognize the fact that the cohesive strength is definitely higher than the so-called tensile strength.

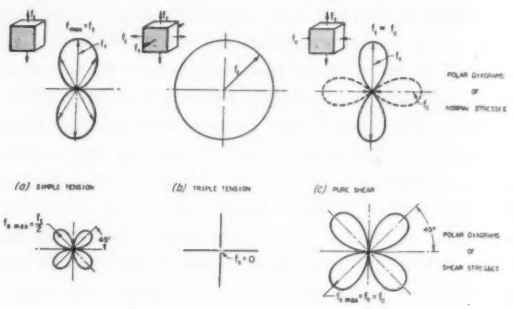

FIG. 8. Stress distribution diagrams.

Since it does not seem practicable to test a material under uniform triaxial tension (at least a piece long enough to obtain stress-strain measurements), the nature of the stress-strain diagram can only be speculated upon. The value of E (ratio of tensile stress in one direction to strain in the same direction) may be estimated from the compressibility characteristics, assuming elastic expansion characteristics to be similar. The ultimate strength may be predicted from work already done on cohesive strength.[5] On this basis we obtain a hypothetic stress-strain diagram such as shown in Fig. 9.

Such a diagram shows at a glance that although the ultimate strength is increased there is no permanent deformation and, hence, no formability. It also shows that the ability to absorb energy (measured by area under diagram) is negligible in comparison with the simple tension condition. This characteristic immediately illustrates the dangerous influence of such combined stress conditions on resistance to impact, a fact that is well covered by the literature but not sufficiently recognized in design.

The practical significance of this discussion of triple tension may be summarized as follows:

(a) Uniform axial stress in all directions (either

* The term "triple tension" will be used to denote equal tensile stresses in all directions. "Triaxial tension" will refer to any condition involving tension along all three principal axes.

tension or compression) prevents permanent set and, hence, represents a condition of zero formability.

(b) This condition also results in almost zero impact resistance, due to the extremely small area under the stress-strain diagram.

(c) The metal, having no permanent deformation under such stresses, would be extremely sensitive to stress concentrations—i.e., would behave like a brittle material.

(d) Any intermediate combined stress conditions tending to approach this extreme case would have, to some extent, the undesirable characteristics predicted above. (This will be discussed later.)

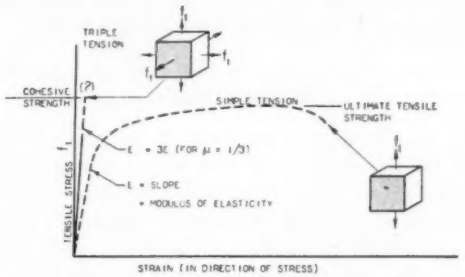

FIG. 9. Comparison of stress-strain diagram for triple tension and simple tension.

SHEAR STRESSES

The preceding discussion indicates that absence of formability is directly connected with the absence of shear stress. In fact, for all practical purposes it may be said that permanent set is entirely the result of shear deformation. Such shear deformation implies a change of shape without change of volume; this is, of course, the result desired in forming.

In the condition of pure shear (Figs. 7c and 8c) there exist planes on which the entire action is a sliding one—that is, there is no tendency toward separation of these planes. If the material is subjected to a shear stress great enough to cause such sliding action the result will be a permanent set without failure, provided that the loading condition does not also produce, on some other plane, normal stresses great enough to cause separation.

In the case of simple tension (Figs. 7a and 8a) shear stresses are present in combination with normal stresses and have a maximum value equal to one-half the maximum tensile stress. Hence we can expect some permanent deformation if the resistance to separation is at least twice as great as the resistance to shearing. The slippage due to shear stresses causes the stress-strain diagram to deviate from a straight line; hence, the plastic portion of the diagram may be thought of largely as a shearing phenomenon (Fig. 1). In a brittle material the resistance to shear deformation is high in relation to the resistance to separation (cohesive strength). Hence, a separation failure may take place before any appreciable permanent set has occurred.

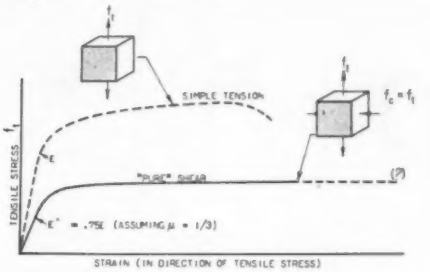

FIG. 10. Comparison of stress-strain diagram for pure shear (two dimensional) and simple tension.

For maximum elongation under simple tension it therefore appears that the material should have a low ratio of shear resistance to cohesive strength.

PURE SHEAR

In the case of pure shear (Figs. 7c and 8c) the maximum shear stress is equal to the maximum tensile stress. Hence, we can expect the shear deformation to proceed much farther than in the case of simple tension before separation occurs. Furthermore, the existence of maximum shear planes having no normal components of stress permits the sliding action to take place on these planes without any tendency toward separation.

To construct a stress-strain diagram for pure shear the maximum tensile stress against the elongation (in the direction of this stress) can be plotted. The value of E (slope) will be reduced by the influence of the compressive stress, which increases the strain measured along the tension direction. (This may be readily calculated for either the two-dimensional or three-dimensional case by using Poisson's ratio.) The proportional limit and the yield point will be lowered, as the critical shearing stress will occur at a lower relative value of tensile stress. Comparing the case of pure shear (Fig. 8c) it can be seen that only half the tensile stress is required in order to produce the same value of shear stress. Hence, it can be expected that the proportional limit and yield stress will be reduced to approximately half their normal values.[6,7]

The elongation will be greatly increased; hence, the stress-strain diagram will be extended over a wide range of strain, as compared with the case of simple tension. These conditions are illustrated in Fig. 10.

If, in addition to the tensile stress on two opposite sides of the specimen, compressive stresses exist on the other four sides (or uniformly around a cylindric specimen) even greater formability can be expected—that is, a longer stress-strain diagram. This condition might be called "three-dimensional pure shear," in which actual separational failure is unlikely.

Finally, a special case can be considered, applying only the compressive stresses uniformly distributed around four sides of the specimen, the other two sides being free. This will produce shearing stresses and

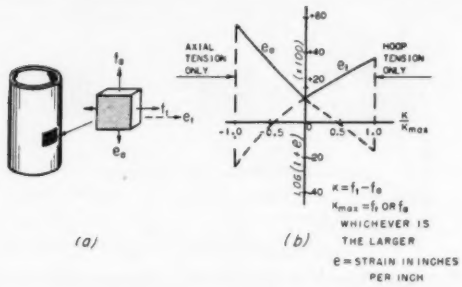

FIG. 11. Biaxial stress conditions. Reproduced from reference 6.

will cause extension normal to the plane of the compressive stresses. Such a stress condition is utilized in the *extrusion* process and is obviously a most favorable forming condition. Since it does not apply to sheet metal forming, however, it will not be considered further in this paper.

INTERMEDIATE CONDITIONS

In the foregoing discussion certain extreme or ideal conditions were used to emphasize the importance of combined stresses. In actual forming work such conditions are seldom, if ever, realized and it is therefore of interest to investigate intermediate conditions. Such intermediate conditions have, in fact, formed the basis for most experiments seeking to determine the true physical properties of materials.

For instance, references 6 and 7 report some interesting results for combined two-dimensional stress. A thin-walled tube was subjected to combined axial tension load and internal pressure. This caused a biaxial tension stress condition in which the ratio between the two tension stresses could be varied. Fig. 11a shows the nature of the test and Fig. 11b shows the test results. The strain at failure is plotted against the difference in stresses, using a nondimensional factor, $K/K_{max.}$, where K is the difference between the circumferential stress and the axial stress. As this factor varies from -1.0 to $+1.0$, the stress condition varies from simple axial tension to simple circumferential (hoop tension). Fig. 11b shows clearly that the strain at failure (measured along the axis) decreases as the biaxial stress is increased, reaching a minimum when the two tension stresses are equal—i.e., when $K/K_{max.} = 0$. (The dotted lines represent strains produced by the stress which did not cause failure.)

The biaxial tension condition can easily be obtained in forming sheet metal and should obviously be avoided if maximum formability is desired. The effects of Poisson's ratio are important in this connection and will be further discussed under that subject.

A sharp notch in a tension specimen has the effect of building up a triaxial stress condition at the root of the notch due to the restraining influence of the unstressed metal adjacent to the notch. This principle has been used by many investigators[9] in attempting to measure the cohesive strength of metals. From a practical standpoint the notch effect is something that should be avoided in design, since it decreases both formability and impact resistance.

In order to be able to predict the complete behavior of metals under stress, it will eventually be necessary to know not only the cohesive strength and the maximum shearing strain (for pure shear) but also to know how these characteristics are affected by the presence of one or the other type of stress. The influence of other factors, such as temperature, rate of loading, etc., will obviously have to be included also. Some information is already available, but further discussion of this interesting subject is beyond the scope of this paper.

POISSON'S RATIO

The tendency of a material to contract laterally under tensile strain (and vice versa) is expressed by Poisson's ratio, which must be regarded as one of the fundamental physical properties affecting forming. The significance of Poisson's ratio is so simple that no further discussion seems necessary at this point. It should perhaps be emphasized, however, that this is a strain ratio, not a stress ratio. Thus, if Poisson's ratio is equal to 0.3 (approximately correct for most metals), it means that a tensile strain is accompanied by a unit contraction equal to three-tenths of the axial strain. This lateral contraction is a strain such as would be produced by compressive stresses acting normal to the direction of tension, but such compressive stresses are not necessary to cause this contraction.

Conversely, if the material is restrained from contracting, such restraint will cause tensile stresses to act normal to the direction of the applied tensile stress. This is an important effect in forming work, as it leads to a double-tension condition which approaches the highly undesirable triaxial tension case and, hence, reduces formability. The unfavorable influences of Poisson's ratio are therefore reflected largely in the tendency to induce tensile stresses normal to the direction of stretching if the sides or edges of the material are prevented from contracting. Such effects are particularly noticeable in the case of wide sheets being stretched or bent.

Poisson's ratio should be regarded as an elastic characteristic. In the plastic range the ratio of dimensional change may be thought of more as a constant-volume phenomenon—that is, the metal behaves much like a fluid. For constant volume, Poisson's ratio would have to be (very nearly) 0.5 instead of the usual value of about 0.3. This means that in the plastic range there is a *higher* rate of lateral contraction during axial extension; hence, freedom to contract in at least one dimension is essential. If one lateral dimension is held constant, the other will have to contract twice as rapidly for constant volume; the plastic value for Pois-

son's ratio would then approach 1.0. In the case of the membrane under full lateral restraint the thickness would therefore have to decrease at the same unit rate at which the length of the membrane increased. Under double tension in which the two tension stresses are equal the decrease in thickness would be approximately twice the value of either tensile strain.

Summary of Forming Properties

Fig. 12 has been prepared to summarize the most important properties and characteristics of the three basic stress conditions.

Referring to this figure, the areas under the stress-strain diagrams (line b) have been shaded to illustrate the marked difference in elongation and energy absorption. The diagram for simple tension is drawn for two different gauge lengths to indicate the large effect on elongation.

The stress intensity at any angle is shown by the diagrams in lines c and d, the first indicating normal stresses (i.e., tension or compression) and the second, shear stresses. Note that in simple tension the maximum shear stress occurs at an angle of 45 deg. and that there is also a normal component of stress on this plane. In case II (triple tension) the axial stress is the same on all planes, but there is no shear stress on any plane. In case III the maximum shear stress is twice as great as for case I, but there is no axial stress component on the 45-deg. plane.

Line e represents the same conditions by means of Mohr's circle. The essential difference is that twice the angle is used in determining points of combined stress on the circle. Thus 90 deg. on the Mohr chart represents 45 deg. on the actual specimen.

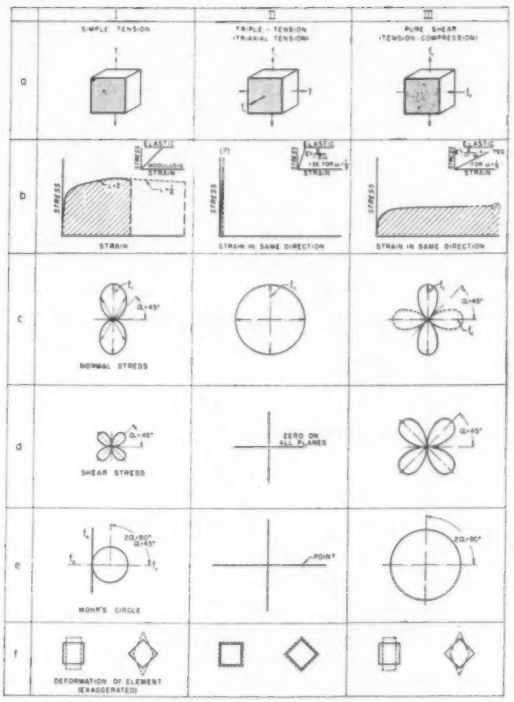

Fig. 12. Comparison of basic stress conditions.

In line f the nature of the deformation is indicated by means of dotted lines. Note that the presence of shear strains is best indicated by an element that is rotated 45 deg. from the line of principal axial stress. It is important to realize, however, that the two different pictures shown for each stress condition actually represent identical strain conditions.

Part 2
Forming Processes

The foregoing discussion indicates the most important physical properties and relationships which affect the behavior of metals under forming conditions. Specific forming operations for sheet metal will now be discussed. Such operations may be broadly classified with reference to the neutral plane of the sheet (in the following definitions the term "element" may be thought of as a small square portion of the sheet such as would be defined by a gridwork of lines scribed or printed on the sheet before forming): (a) *bending*, which involves no change in the area or relationship of elements in the neutral plane of the sheet; (b) *stretching*, in which the primary action is the *elongation* of elements in the neutral plane, bending and shear stresses being secondary; (c) *compression*, the opposite of stretching, sometimes called shrinking; (d) *drawing*, in which the predominating action is a shearing deformation produced by a combination of stretching and compression. In the ideal case these would change in shape but not in area.

These general types of forming will now be discussed in greater detail as they occur in actual manufacturing.

Bending

Simple bending occurs when the final (bent) surface is a developable one—that is, the bent portion must form a portion of a cylinder or a cone. Since this is one of the easiest types of forming which can be done, the designer should make every effort to use developable surfaces if sheet metal is involved. This point should be obvious, but actual experience indicates that it is often overlooked by the draftsman.

The two major problems in simple bending are minimum bend radius and spring-back. The first is of special interest in the forming of flanges and sheet metal stringers. A large bend radius is usually structurally inefficient, since it leads to a wide flange which in turn causes lower buckling stresses under compressive loading. The problem of minimum bend radius was given

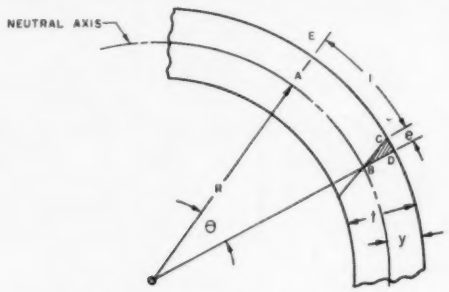

FIG. 13. Geometry of a bend.

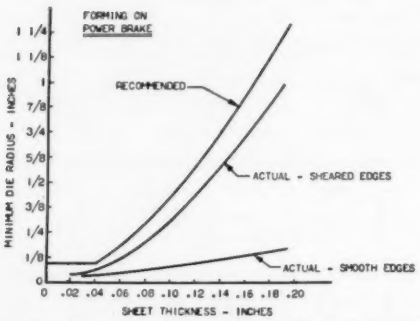

FIG. 14. Minimum bend radii for various sheet thicknesses (24ST Alclad 90-deg. flange).

special attention at Lockheed because of the conversion of a large number of aluminum alloy parts from SO forming to ST (high-strength) forming. It was desirable to avoid changing the form block bend radii any more than necessary to allow for the different properties of the heat-treated and annealed materials.

The most practical way to determine the minimum bend radius is to make actual tests and thus obtain empiric data. However, it is desirable to be able to analyze the problem and thus be able to predict the minimum bend radius from a knowledge of the basic physical properties, especially when new materials are involved.

If a flat piece of metal is bent to conform to a cylindric surface having a radius R_0, the conditions will be similar to those of Fig. 13. The line AB represents a unit length measured along the neutral axis. From the definition of the neutral axis, this length will not change during bending. The corresponding length of the outer fiber will increase by the quantity e, which is the tensile strain or elongation. Because of the continuity of the sheet on either side of the bent portion, cross sections that were plane before bending will tend to remain plane after bending. This justifies the assumption of a linear strain distribution across the sheet thickness, in accordance with the classic beam theory. Such a variation of e is shown by the line BC, drawn parallel to line AE.

From the similar triangles OAB and BCD the following simple relationship is obtained.

$$e/y = 1/R \qquad (1)$$

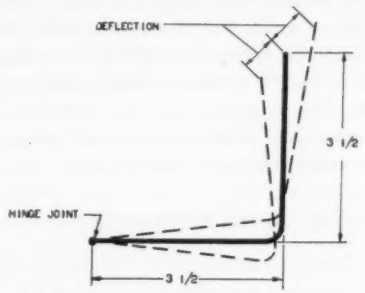

FIG. 15. Vibration test on bend.

or

$$e = y/R \qquad (1a)$$

If the neutral plane is assumed to be the midplane of the sheet, $y = t/2$ and Eq. (1a) becomes

$$e = t/2R \qquad (1b)$$

For cases in which y is not equal to $t/2$ the formula may be generalized by expressing, $y = Kt$, giving the equation

$$e = Kt/R = K/(R/t) \qquad (1c)$$

(K will usually be very nearly equal to 0.5 but may vary somewhat, depending on the nature of the bending process.)

Now, if the allowable elongation $e_{max.}$ is known, the minimum value of R/t may be found by substitution of $e_{max.}$ in Eq. (1c).

$$(R/t)_{min.} = K/e_{max.} = 1/2e_{max.} \text{ (approx.)} \qquad (2)$$

To determine $e_{max.}$ it is necessary to consider the length being stretched, as already described under local elongations. Thus, for a right-angled bend of a thin sheet, the length being stretched may be in the neighborhood of 0.25 in. From Fig. 4 the maximum elongation for this length is found to be 0.34. Substituting in Eq. (2) a value of R/t equal to 1.47 is obtained. To be exact, a check should be made of the assumption for the length being stretched, which is simply the length of the arc measured along the neutral plane:

$$L = (\theta_t/360)2\pi R = \theta_t R/57.3 \qquad (3)$$

where θ_t is the total arc of bending and is measured in degrees.

Values of minimum bend radius predicted in this manner are in good agreement with test results when the edges of the blanks are free from burrs. If edges are left with serious burrs, such as caused by shearing, the minimum forming radii may in some cases be much greater than those determined for smooth edges. This is particularly true for heavy gauge material which, when bent too sharply with sheared edges, develops edge cracks so extensive as to involve rejection of the part or a good proportion thereof.

Typical curves of minimum bend radius plotted against sheet thickness are shown in Fig. 14 for 24ST

FIG. 16. Enlarged view of cross section through typical bends.

Alclad material formed on the power brake.[20] When forming is done on the hydropress with rubber punch, these values will be conservative. As indicated in Fig. 14, a margin of safety is usually introduced for design purposes by using a larger radius than the minimum test values. This allows for variations in the materials and manufacturing technique.

It was felt that bending to the smallest possible radius might adversely affect the fatigue properties of the bent metal. Special fatigue tests were therefore conducted in which bent strips were vibrated as indicated in Fig. 15. The results indicated that no danger need be expected from this source if the material had not already been cracked by the forming process. Small cracks on sheared edges showed no appreciable tendency to spread during this test.

Strain Distribution in Bends

For simplicity, the strains on outer and innermost fibers are frequently assumed to be distributed uniformly around the bend; however, measurements have shown the actual distribution to be nonuniform. Fig. 16 shows an enlarged view of the cross section of a bend. The serrations appearing in this figure were produced by rolling grooves into the soft outer layer of aluminum before bending. (These serrations had an original uniform spacing of 0.01 in.) Strains in bends are obtained by measuring the change in spacing of the serrations due to forming. The strain distribution in a typical bend is shown in Fig. 17.

The nonuniformity of strain distribution has to be taken into account in accurate calculation of minimum

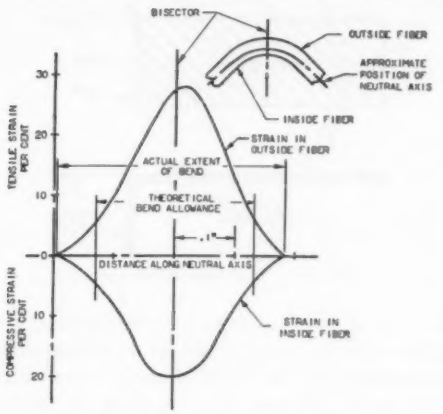

FIG. 17. Strain distribution in a typical bend.

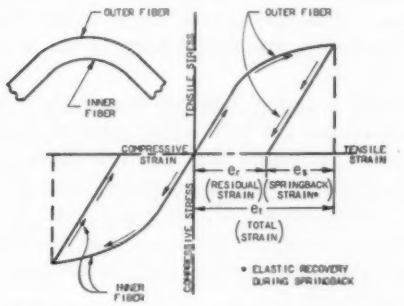

FIG. 18. Stress-strain conditions during bending (outer fibers).

bend radius, bend allowance, and spring-back. In particular, it affects the determination of the actual position of the neutral axis.

Spring-Back

Spring-back may be defined as the process of elastic recovery which occurs when the pressure being exerted to form a part is removed. It is usually expressed in terms of the angular change $\Delta\theta_l$ and the radius change ΔR which take place. During bending, each longitudinal fiber undergoes a tensile or compressive strain proportional to its distance from the neutral plane, as illustrated in Fig. 13. If we were to consider only the outer fibers (which undergo the greatest strain), the stress-strain picture would look something like Fig. 18.

In this figure the conditions at the outer fiber are pictured as starting from zero on the S-S diagram, following the regular tensile and compressive curves to the point of maximum bending strain (e_t) and recovering elastically to zero stress at the permanent or residual strain (e_r). If this were exactly true, the ratio between the initial and final radii of curvature would be in proportion to the ratio e_r/e_t (Eq. (1b)). This quantity therefore represents (approximately) the completeness of the forming—that is, the degree of permanent bending which may be expected. It will therefore be designated by the symbol F, called the "forming

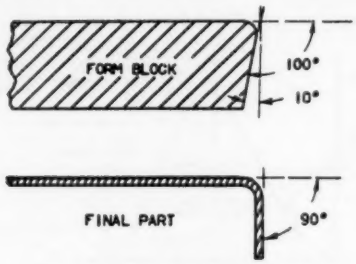

FIG. 19. Overforming of a part.

factor." If F were equal to 1.0, the forming would be complete—that is, there would be no spring-back.

For example, if a flat piece of metal is bent through a total arc of 90 deg. and then released, the final degree of bend will be $90F$. Thus if F were equal to 0.9, the final part would have a flange angle of 81 deg., instead of 90 deg. The spring-back angle would then be 9 deg. (Obviously, the spring-back factor is equal to $e_s/e_t = 1 - F$.)

To obtain a desired final bending angle it is necessary to overform the part. The value of F must first be found or estimated and the form-block angle (or angle to which part is bent in brake) must then be increased by the factor $1/F$. If it is desired to obtain a final angle of 90 deg. in the above case, the form-block angle must be made equal to $90/F = 100$ deg. This is illustrated in Fig. 19.

The forming radius would also change due to spring-back. The factor F may be used in the same manner to estimate this change, but in the case of small 90-deg. bends the actual differences involved are usually small enough to be neglected. For parts having a shallow contour of large radius the change in R must be allowed for in form block design.

The foregoing discussion shows that spring-back is largely a function of the forming factor, $F = e_r/e_t$. To determine this factor analytically becomes a complicated matter if all the variables are considered. For instance, the assumption of a linear strain distribution requires that the same proportionality of strain be maintained at all times for each internal fiber. Fig. 20 has therefore been drawn to indicate what happens to fibers within the specimen.

In this figure we consider five different fibers, starting with zero at the neutral axis and equally spaced.

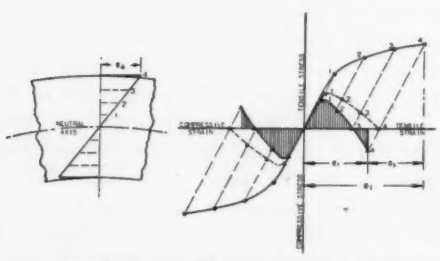

FIG. 20. Equilibrium conditions for all fibers during bending (showing conditions after spring-back).

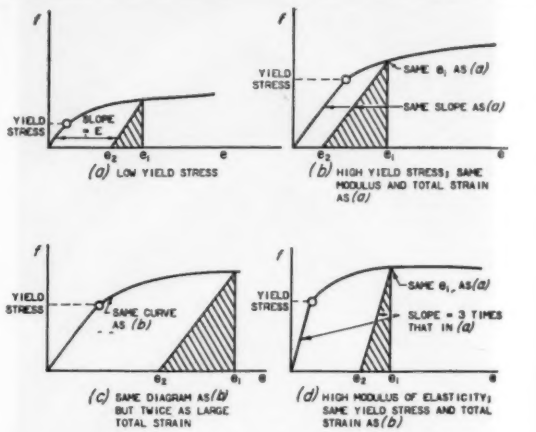
f
YIELD STRESS
SLOPE = E
e
(a) LOW YIELD STRESS
SAME θ_1 AS (a)
SAME SLOPE AS (a)
(b) HIGH YIELD STRESS; SAME MODULUS AND TOTAL STRAIN AS (a)
SAME CURVE AS (b)
(c) SAME DIAGRAM AS (b) BUT TWICE AS LARGE TOTAL STRAIN
SAME θ_{1_2} AS (a)
SLOPE = 3 TIMES THAT IN (a)
(d) HIGH MODULUS OF ELASTICITY; SAME YIELD STRESS AND TOTAL STRAIN AS (b)

FIG. 21. Effect of shape of stress-strain diagram on spring-back (e_1 = total strain; e_2 = residual strain; $e_1 - e_2$ = spring-back strain; $F = e_2/e_1$ = forming factor; E = modulus of elasticity).

If the sheet is bent to cause a total strain e_4 at the outer fiber, the inner fibers will be spaced along the S-S diagram as shown. Now, if each of these returned to a condition of zero stress along the elastic recovery lines, proportionality of strain would no longer exist. Hence, the simple method of considering only the outer fibers must be regarded as a round approximation.

The final state of the bent member must comply with two conditions. First, the strains must remain linear—i.e., points 0 to 4 must remain evenly spaced on the strain scale. Second, the final internal stresses must represent a resultant bending moment of zero. If we make the assumption that the outer fiber (point 4) returns to zero stress (as done in Fig. 18) and then divide the corresponding strain into four equal parts, the intermediate fibers would be located as indicated by the squares on Fig. 20.

The corresponding stresses then produce an unbalanced bending moment which, of course, could not be present after forming. Hence, the outer fiber will have to spring back still more, following its elastic recovery line. This puts it in the compression range. The final solution will be obtained when the area under the residual stress-strain diagram has zero moment about the origin. The curve through the triangular points of Fig. 20 represents the final stress distribution satisfying this requirement and thereby determines the final value of e_r.

The value of residual strain at the outer fiber determined in this manner will be less than that obtained from the approximate method previously described; hence, the spring-back will be greater. The final conditions will involve residual compressive and tensile stresses in the part as shown. A typical curve found from experiments is given in reference 13 and agrees quite well with the above theory (developed at Lockheed in 1940 by E. I. Ryder).

Further refinements of the theory have been made to

include the effects of nonuniform strain around the bend and similar factors. These will be reported in a separate paper, which will include practical examples.

The same general theory will apply to cross sections other than rectangular, except that the final equilibrium computations must take into account the nonuniform distribution of area about the neutral axis. This is well covered in reference 12.

To obtain a better idea of the relative importance of various factors affecting spring-back, the approximate method of evaluating the factor F can be used, applying this method to different types of materials and forming conditions. Fig. 21 shows such a comparison from which the following general conclusions may be drawn: (a) a low yield stress gives a high value of F, hence increases formability and reduces spring-back; (b) a high yield stress reduces the value of F, hence causes more spring-back; (c) a higher total strain (e_t) increases the ratio F, hence reduces spring-back; (d) for the same yield stress, higher values of E reduce the spring-back.

Item (a), of course, explains why spring-back can be practically eliminated by using annealed material (low yield stress). Item (b), conversely, shows why spring-back becomes a problem when working with high-strength material. Item (d) appears surprising at first sight, since it might be expected that a high modulus of elasticity would increase spring-back. The opposite is actually the case, provided that the yield stress remains constant. Actually, the yield stresses for different high-strength materials are roughly proportional to E; hence, the effects tend to cancel out and the spring-back is not affected so much as might be supposed. Raising the yield stress by work-hardening will increase spring-back, since E is not raised by this process.

Item (c) explains the well-known fact that for the same bending radius there is less spring-back for thick sheets than for thin ones. This is because the strain required at the outer fiber is proportional to the sheet thickness (Eq. (1b)). Thus, for sheets of the same weight and bend radius the spring-back will be greater for the high-strength materials primarily because they are thinner—not because of the different moduli of elasticity.

As a corollary to item (c), a small amount of bending may fail to produce a maximum strain that is equal to, or appreciably greater than, that corresponding to yield stress. Under such conditions the spring-back may be very large or may even be complete (i.e., no permanent forming). Hence, it can be concluded that for a given material and sheet thickness there will be a limiting bend radius above which no permanent set will be produced. This condition is often found in aircraft construction where thin sheets are used to form slightly curved surfaces (fuselages and wings, for example). In such cases the sheet may sometimes be bent elastically to the desired contour on assembly. It is, however, possible to preform such sheets by special methods, such as stretching or rolling, both of which produce strains in the plastic region.

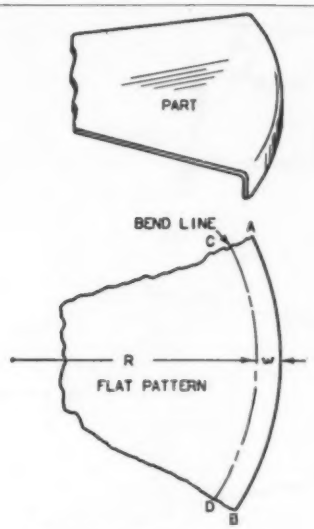

FIG. 22. "Shrink" flange.

An interesting sidelight on the above remarks is the case of the spring, in which the desired conditions are the reverse of those in forming. Hence, springs are made from thin sheets or wire subjected to low bending stresses (large R/t values) and from materials that have a high yield stress.

BENDING OF CURVED FLANGES

If the bend-line for a flange is not straight, the bending of the sheet will introduce stretching or compression effects in the midplane. This is illustrated by Fig. 22, which shows the flat pattern for a typical part before bending. This type of flange may be called a shrink flange, since the free edge AB must be made shorter as the flange is bent to its final position immediately below the bend-line DC.

Fig. 23 shows a stretch type of flange in which the free edge must be lengthened during the bending process.

Applying the same geometric reasoning that was used in the elementary bending problem, the approximate degree of shrink or stretch may be calculated from the ratio w/R, where w is the width of flange and R is the radius of the bend-line in the plane of the flat pattern.

If it is assumed that the length along the bend-line CD does not change, the strain (unit change in length) of the free edge will be given directly by the ratio w/R:

$$e = \frac{w}{R \pm w} \cong \frac{w}{R} \tag{4}$$

Actually, the length CD will tend to change in the opposite direction from the change in AB but is more or less restrained by the adjacent material. Eq. (4) may therefore be regarded as sufficiently accurate for practical purposes. If the bend-line does not have a

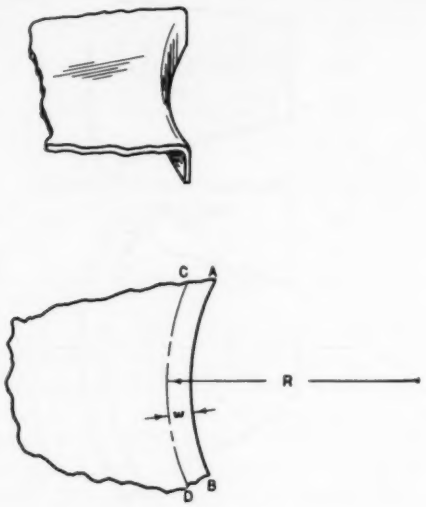

FIG. 23. "Stretch" flange.

constant radius, the local radius over a suitable short length may be used to compute the local strains.

In the case of a stretch flange, Eq. (4) may be used to compute the tensile strains in the free edge. These must then be compared with the maximum permissible strain for the material, with proper allowance for the length involved (Fig. 4). On this basis it is possible to predict, for a given material, the limiting value of w/R above which cracking of the free edge will occur.

For 24SO and 24ST aluminum alloys the amount of stretch that may be used in Eq. (4) will vary from 10 to 22 per cent for 2-in. lengths and from 25 to 40 per cent for $^1/_4$-in. lengths. The higher values are obtainable under ideal conditions, while the lower ones are more suitable for high production.

After a curved flange has been bent to shape, the stretch that occurs in the free edge will cause a secondary spring-back effect which tends to bow the flat portion of the part. This can be explained and predicted by the same principles used in the analysis of ordinary spring-back. The direction of the bow will, of course, depend on whether the free edge is being stretched or compressed.

Shrink flanges (Fig. 22) involve compressing the free edge of the sheet; therefore, there will be a tendency for the sheet to buckle as a column. It is possible to compute the critical buckling stress for a flat sheet supported at one edge. However, such calculations are not so valuable as direct experimental data because of the various assumptions that must be made, as well as the influence of the actual forming conditions.

It has been found that 24SO material can be shrunk about 6 per cent in the single-acting hydropress using a form block and a rubber platen. 24ST can hardly be shrunk at all under such conditions, since the rubber does not give sufficient support against buckling.

Before taking up specific details concerning shrink flanges, it may be well to note a few general points that may be of some practical interest. Since the critical buckling strain is a function of the ratio w/t, it follows that a greater amount of shrinking may be done on thicker sheets before buckling occurs, other things being equal. As in the case of spring-back, the influence of sheet thickness is found to be of more importance than the difference in physical properties in dealing with high-strength materials. The slope of the stress-strain curve in the plastic range is a function of the "local" or "tangent" modulus of elasticity rather than the initial (Young's) modulus. To compress the material into the plastic range without buckling requires that the tangent modulus (local slope) remain high enough to prevent buckling under the stress corresponding to the strain developed. This leads to an interesting index of formability reported in the Appendix.

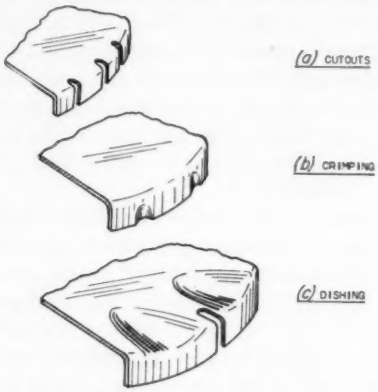

FIG. 24. Treatment of "shrink" flanges.

CUTOUTS AND DISHES

A common method of avoiding the buckling of shrink flanges is simply to cut out pieces of material, thereby permitting the shortening of the flange edge without building up appreciable compressive stresses (Fig. 24a). Another way is to form the buckles into the sheet as a deliberate design feature, a process that might be called "crimping" (Fig. 24b).

A different approach to the problem has recently been developed at Lockheed.[19] This consists in "dishing" the flat part of the sheet as shown in Fig. 24c. The underlying principle, which might be called the "reflection principle," is simple and valuable. The portion to be bent (usually a flange) is first viewed in the flat pattern and then assumed to be bent into the final position without change of shape. If this can be done, no stretching or shrinking will be required. Unless the bend-line is straight, however, its flat-pattern shape will now appear in the end view and will require forming of the initially flat part of the sheet to conform. Fig. 25 illustrates how this works for a simple shrink flange, in which the compressive strains are eliminated by designing the part to have a bowed web. If the

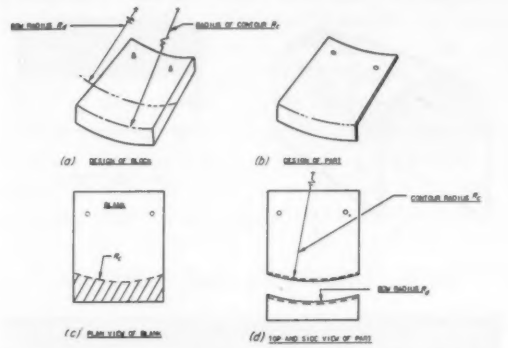

FIG. 25. Elimination of compressive strains by bowed web.

radius of the bow is made equal to the contour radius of the flange, the shaded portion of the blank (Fig. 25c) is nearly equal in shape and area to the material in the flange (Fig. 25d), while the unshaded area of the blank is very nearly equal in shape and area to the web. It follows that only slight plastic distortions other than simple bending are necessary to form the part.

If the flange is long, it must be cut into several shorter lengths with cutouts, as shown in Fig. 26a. The principle of bowing is then applied to each length between cutouts. It has been found by experience that the part need not be bowed across the whole web but that the bowed region near the flange may be faired into the flat web, approximately as shown in Figs. 26b and 26c.

Special machines have been devised to facilitate the forming of flanges and other parts that undergo shrinking. These usually work on the principle of progressive forming, in which the compression of the sheet is done over short lengths and, hence, does not cause buckling. Other methods, such as dies and draw rings, use the principle of supporting the sheet against buckling during compression.

JOGGLES

Joggles represent an acute case of curved flanges, in which the curvature is severe and highly localized. Since joggles are widely used in the design of sheet metal parts, special efforts have been made to facilitate their production in the shop. Even in the forming of annealed material on the hydropress it is frequently necessary to hand-set the joggles after forming because of the inability of the rubber pressure to produce the necessary local changes of shape. This trouble is not a bending problem but is due to the stretching and shrinking strains that must be produced to form the double-curved part.

The reflection principle may be used to advantage in forming joggles. The application of this principle in the forming of a double joggle is illustrated in Fig. 27. Note how the bent portion (shown shaded) has the same shape before and after bending. Another application

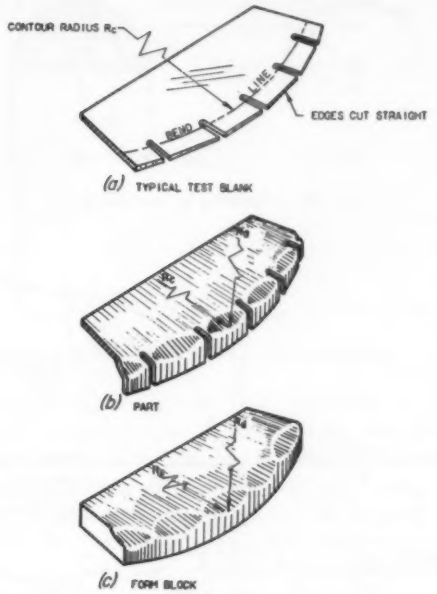

FIG. 26. Dished construction for part with flange cutouts.

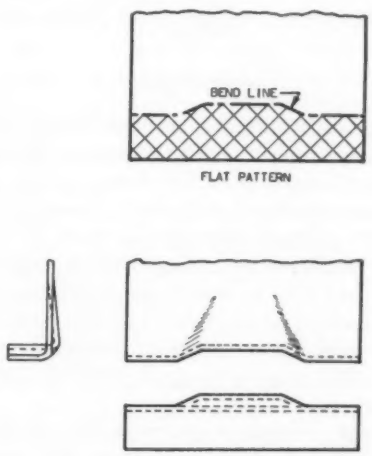

FIG. 27. Use of reflection principle in forming double joggle.

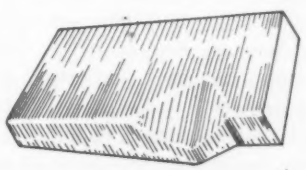

FIG. 28. Form block for single joggle.

is shown in Fig. 28, which represents a form block used for a single joggle.

Other practical methods of improving the forming of joggles have also been developed and are in use at Lockheed, but since they do not involve elastic theory they are considered beyond the scope of this paper.

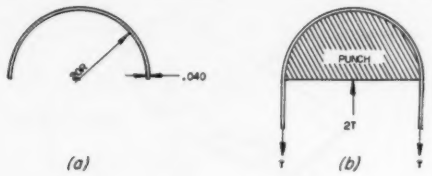

FIG. 29. Forming by stretching.

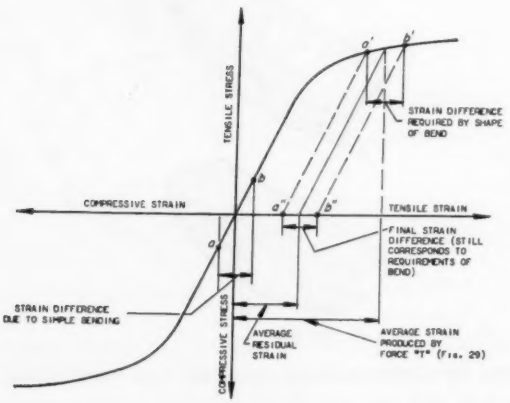
TENSILE STRESS
COMPRESSIVE STRAIN
TENSILE STRAIN
COMPRESSIVE STRESS
STRAIN DIFFERENCE REQUIRED BY SHAPE OF BEND
FINAL STRAIN DIFFERENCE (STILL CORRESPONDS TO REQUIREMENTS OF BEND)
STRAIN DIFFERENCE DUE TO SIMPLE BENDING
AVERAGE RESIDUAL STRAIN
AVERAGE STRAIN PRODUCED BY FORCE "T" (FIG. 29)

FIG. 30. Stress-strain conditions during bending and stretch-forming.

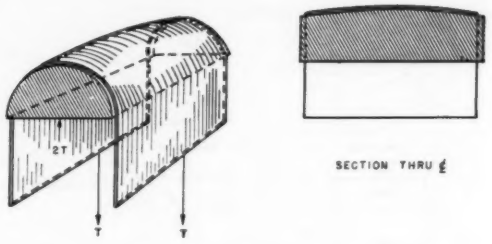

FIG. 31. Stretching to a doubly curved surface.

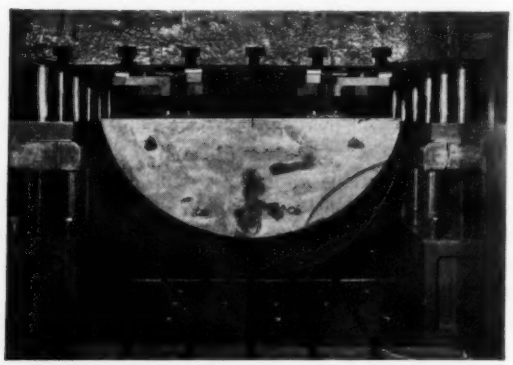

FIG. 32. Use of double-acting press in stretch-forming.

STRETCHING

This type of forming has been previously defined in this paper as a type in which the primary action is the elongation of elements in the neutral plane, bending and shear stresses being secondary. Since there would be no point in simply stretching a sheet, it is obvious that stretching must be accompanied by some bending if any forming is to be done. As already noted under bending, a large value of R/t will not cause bending stresses beyond the elastic limit; hence, in such cases it is impossible to form the part simply by bending it to contour.

As a simple example consider the case shown in Fig. 29. The value of R/t is $20/0.04 = 500$. From Eq. (1b) $e = 0.001$.

For aluminum alloy this would correspond to a stress of $0.001 \times 10{,}000{,}000 = 10{,}000$ lbs. per sq.in. (approx.). Such a stress obviously lies below the yield stress for heat-treated aluminum alloy, indicating that forming by simple bending would be practically impossible (unless rolls were used to produce a high degree of localized bending). The conditions just described are shown in Fig. 30, in which the stress conditions on the inside and outside surfaces of the sheet are represented by a and b, respectively, the strain along the neutral axis being zero. Upon release of the bending forces, the recovery would be purely elastic and the bending strains would disappear entirely (a and b would both return to zero).

It is possible to eliminate spring-back very effectively by a combination of bending and stretching as shown in Fig. 29. Assume that a force T is applied such as to produce a tension strain well into the plastic range. This strain is superimposed on the bending strains, producing conditions at the surfaces of the sheet such as indicated at a' and b' in Fig. 30. If the tension load is removed, the recovery will be as indicated by the dotted lines. Note that the residual difference between strains a'' and b'' remains almost identical to that between strains a and b required by simple bending; hence the sheet will remain curved after the stretching force is removed, with spring-back virtually eliminated.

In the foregoing examples the part was assumed to be formed to a cylindric surface. The same principles apply when the surface has double curvature, as shown in Fig. 31. The only difference is that the amount of stretch will vary between the centerline of the sheet and the edges. The limits to which this process may be carried will therefore depend on the maximum stretch required at any point in order that *all* points are strained into the plastic range. If the maximum tensile strain should exceed the allowable value at any point, the material will, of course, fail in tension. Hence, a material having a high maximum elongation over a large gauge length must be used in severe cases.

A part having *one* deep contour of curvature, the other being relatively shallow, can be formed by stretching as described above. But if the part is deeply contoured in *both* directions, it is impossible to obtain enough stretching action without tearing the sheet; hence, the part must be formed by the drawing process to be described later in this paper.

Forming problems of this type may be approached by analytic methods. It is usually possible to calculate or measure the maximum difference in length of the tension elements in the formed condition. This difference, divided by the smaller developed length over which it was measured, indicates the difference in tensile strain that must be applied during forming. Obviously, this figure must be somewhat less than the maximum permissible elongation of the material, the exact amount depending on how close the ultimate (breaking) strain may be approached in the particular forming process used.

An exact analysis would be quite complicated, since it would be necessary to take into account the biaxial tension condition, form block friction, and other variables. Nevertheless, the foregoing principles may be used to interpret the results of tests and to eliminate many costly experiments that could not possibly be successful.

Fig. 31 shows a part with double curvature such as would be found in a wing-tip or cowling ring. Parts having a small amount of double curvature are also troublesome, especially since it is usually desirable to form them in the high-strength condition. The same stretching technique may be used in such cases, but the practical application of the process involves many secondary problems that must be solved largely by trial and error. The use of an extra-depth punch to obtain requisite stretching is often necessary.

At Lockheed[18] the double-acting press has been used to advantage in this work, as it permits the part to be held at the edges while the punch forms it, thereby building up the desired tensile stress. This is illustrated by Fig. 32. The same general effect may be obtained in the single-acting press with rubber punch by the use of special, concave blocks in which the sheet is clamped at the edges by means of beaded plates or other devices. Mechanical stretching machines are also available for this type of work. These methods are generally not so satisfactory as the double-acting press, however, since the latter permits greater control over the edge restraint conditions.

As previously explained, the critical feature about stretch-forming is the danger of exceeding the maximum allowable strain before all portions of the sheet have been stretched to contour. This problem becomes even more serious at the point of attachment of the sheet to the stretching device or press. Since operations are conducted in the plastic range, the tensile stress will necessarily be high and will approach the ultimate stress as the limits of formability are approached. If any material is removed for rivet holes, pins, or other holding devices, the local stresses will be increased appreciably, thus greatly reducing the range of forming that may be done without failure.

Fig. 33 shows diagrammatically how a 15 per cent reduction in joint efficiency would decrease the available forming range. Obviously, it is important to

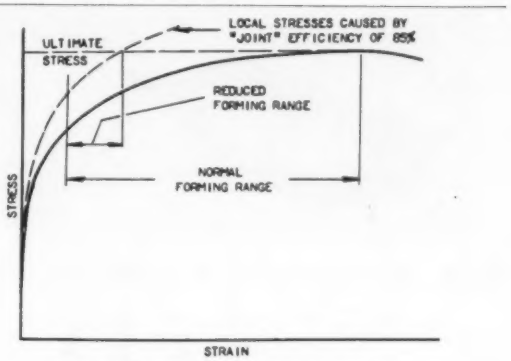

FIG. 33. Joint effect on forming range.

develop the highest possible joint efficiency in stretch-forming. Our experiments have shown that the most efficient method of holding the sheet is to clamp it between two relatively smooth surfaces, using a high normal pressure to keep the sheet from slipping. This method (used in the double-acting press) has the advantage of producing a tension-compression condition in the sheet which increases the allowable elongation (Part 1). There is also a tendency for the sheet to slip locally before it fails because of the "necking down" which occurs at high stresses. This also tends to relieve the stress at critical points. The use of a carefully polished and lubricated punch is essential to successful results.

The range of forming that can be done by this method may be further extended by deliberately permitting the sheet to slip between the clamping plates over certain critical areas. This may be done by proper control of the clamping pressure. The conditions thus obtained begin to approach those of drawing, which will be taken up later.

The effect of Poisson's ratio must be considered in stretching operations. If the sheet were completely restrained on all sides and then stretched in one direction only, transverse stresses would be built up. This biaxial tension condition would tend to lower the permissible elongation as described in Part 1. Hence, it is best to leave two edges free.

The same reasoning applies to the use of the double-acting press. If the punch is used to develop the tension in the sheet, it is generally better to clamp the sheet on two edges only, otherwise the biaxial tension condition will be produced. (This principle does not apply to cases in which the sheet is allowed to slip—that is, where the clamping device is used as a draw ring.) In the case of a double-contoured part, it has been found best to restrain the sheet along the edges that have the smaller depth of contour indicated in Fig. 34.

The adoption of stretch-forming techniques as described above has eliminated a great deal of hand-work with speed hammers, crown rolls, etc., which was formerly necessary in forming double-curvature parts.

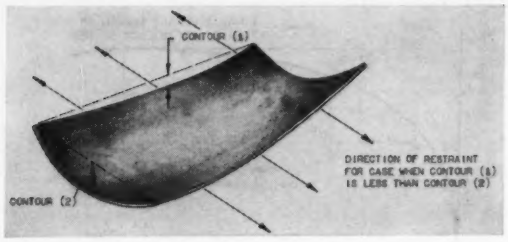

FIG. 34. Direction of restraint for stretching contoured part.

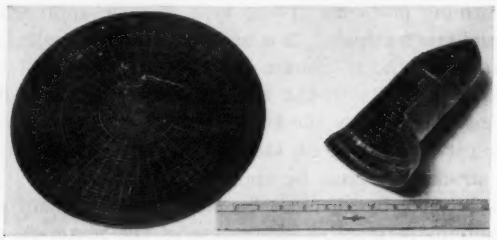

FIG. 37. Drawn cup with polar coordinate grid.[1]

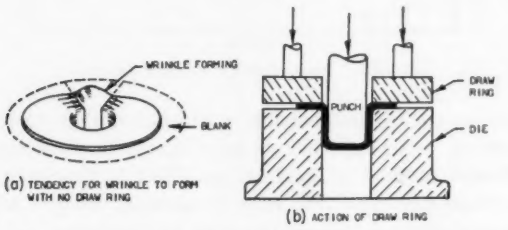

FIG. 35. Prevention of wrinkles by draw ring in double-acting press.[15]

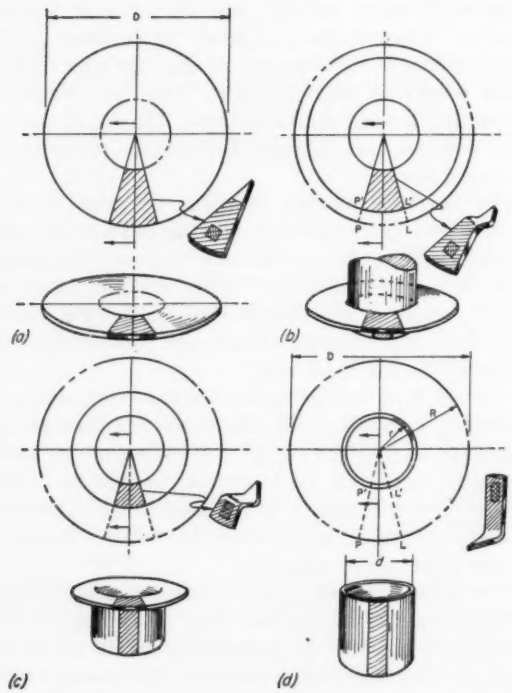

FIG. 36. Stages in drawing a cup.[18]

It has also made possible the forming of shallow-contour parts in hard tempers (24ST Alclad, $^1/_4$H stainless steel) which formerly had to be made in the annealed grades.

DRAWING

As applied to sheet metal forming the term drawing implies that the material being formed is drawn from the unformed portion of the sheet in a progressive operation. More specifically, the term drawing is used to define the class of operations in which stretching in one direction is accompanied by compression normal to this direction. As shown in Part 1, such a condition would represent pure shear if the tensile and compressive stresses were equal. Although this ideal condition is difficult to attain in forming processes, it may be approached to an extent which permits much greater elongations than those usually obtained.

Drawing of sheet metal was a well-developed process in many industries, but its application to high-strength aluminum alloys had not received much attention until recently. Lockheed's research work was therefore directed toward such materials. The results[15] have been most encouraging and have shown that under proper conditions the aircraft aluminum alloys can be severely formed by this process.

The process itself is best illustrated by the drawing of a cup from a flat disc. The most important feature of the process is the use of the draw ring, illustrated in Fig. 35. This ring is forced against the metal just enough to prevent it from wrinkling but not enough to prevent slippage.

As the punch descends, the metal flows around the drawing radius, is elongated along the axis of the cup, and is compressed in a circumferential direction. This action takes place almost entirely underneath the draw ring and around the drawing radius, not in the die itself as might be expected. (This point clearly brings out the importance of the draw ring.) Successive stages of forming are illustrated in Fig. 36, in which a segment of the sheet has been shaded so as to show its change of shape. Note that radial lines become parallel as the material passes around the drawing radius. This has been proved by actual experiments in which a radial gridwork of lines was first photographed on the sheet, as shown in Fig. 37.

Fig. 36 shows what happens to a square element as the drawing process takes place. As the diameter of the blank decreases, the metal is compressed circumferentially and is stretched radially. This causes the square element to become narrower and longer, as indicated by the small shaded area on the segment. Referring to Fig. 12f, Part 1, it is seen that this type of strain is associated with pure shear and results in high allowable elongation in the tension direction.

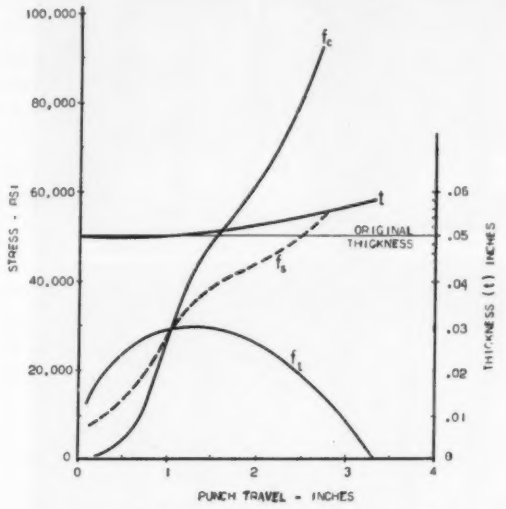

FIG. 38. Stress conditions in drawn cup.[17]

Experiments conducted at Lockheed have produced many interesting results, some of which are noted below:

(a) Tensile strains as high as 110 per cent have been produced in 24SO Alclad without failure. 24ST has been strained 22½ per cent in this manner; 24SRT, 10 per cent.

(b) 24SO Alclad may be drawn into a cup in one operation if the inside diameter of the cup, after drawing, is not less than 55 per cent of the original blank diameter.

(c) Since the forming is done largely by shear deformation (change of shape without change in area), the size of the original blank to be used may be quite closely calculated by figuring the total surface area of the final part.

(d) For unusually deep draws, successive forming may be used, in which a large diameter cup is first produced, then redrawn to a smaller diameter. By using three draws, 24SO Alclad has been successfully formed into a cup having a diameter ratio (cup to blank) of about 36 per cent, without intermediate annealing of the material.

(e) Limited tests indicate that a drawing radius of about five times the sheet thickness is satisfactory for 24SO Alclad material.

(f) Parts of irregular shape (such as window frames) may be considered as being made up from segments of cups, and the same general technique and calculations may therefore be applied. Draw ratios obtainable at the corners are favorably affected by the flow of metal from the adjacent straight sides (see illustrations in reference 16).

(g) Careful design of dies is essential for perfect results. Where large quantities of parts are to be formed, as in the automotive industry, the use of steel proves economical; but if quantities of the order of a thousand or so are involved,[14] materials such as zinc alloy can be used with equal success, provided die design is as carefully carried out as with the steel dies.

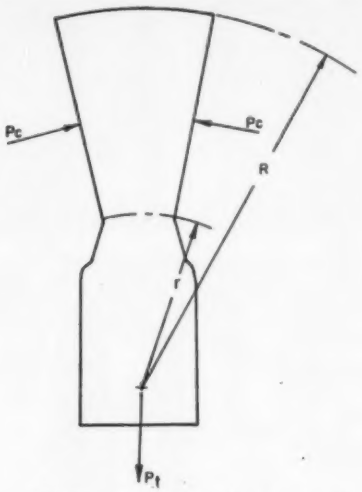

FIG. 39. Wedge draw test coupon.[17]

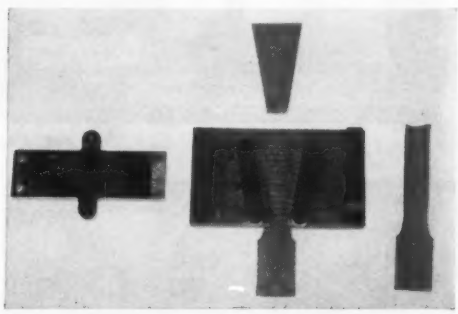

FIG. 40. Wedge draw test jig.[17]

STRESS VARIATIONS IN DEEP DRAWING

Fig. 38 shows how the stress relationships change during the drawing process. The curves, which are derived from theory and test data, show the conditions at the drawing radius. The shear stress f_s is computed by combining the compressive stress f_c and the tensile stress f_t in the plane of the material in a manner already discussed in Part 1. It is the stress responsible for the pronounced plastic flow of the material. There are other shear components introduced by the tensile and compressive stresses, but these are manifested only by a slight effect on thickness as described later.

At first the tensile stress f_t increases quite rapidly, since the punch must exert a large force in order to produce lateral compression of the flat sheet which is under the draw ring. As drawing progresses, the area of the flat sheet under the ring decreases and the tensile stresses therefore begin to decrease also. The first critical condition occurs at or near the point where the tensile stresses are at a maximum. If this condition can be passed through without failure, the operation

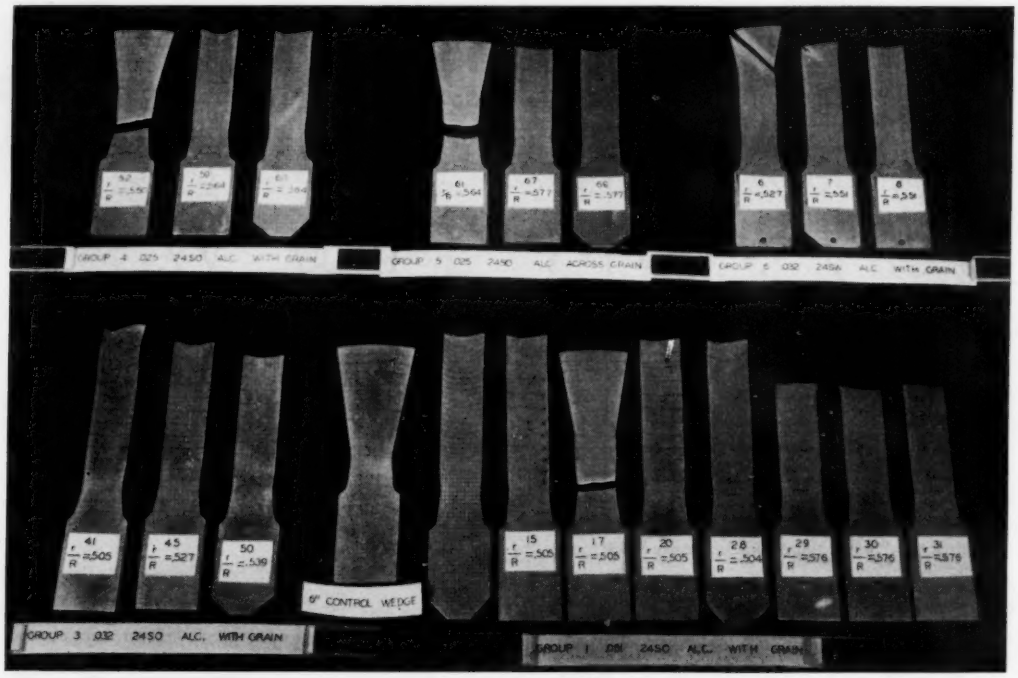
GROUP 4 .025 24SO ALC. WITH GRAIN
GROUP 5 .025 24SO ALC. ACROSS GRAIN
GROUP 6 .032 24SO ALC. WITH GRAIN
9° CONTROL WEDGE
GROUP 3 .032 24SO ALC. WITH GRAIN
GROUP 1 .051 24SO ALC. WITH GRAIN

FIG. 41. Coupons from wedge draw test.[17]

FIG. 42. 24SO Alclad coupon deformed 46 per cent in shear.

will be successful unless a point is reached at which the material in the flange portion fails in shear (see curve f_s, Fig. 38).

Fig. 38 shows that the thickness at first decreases slightly due to a shear component introduced by the high tensile stress. However, the thickness tends to increase toward the end of the operation because of the high ratio of compressive to tensile stress then present. The shear component of the former squeezes the material upward, so to speak, against the opposing thinning tendency of the holddown pressure and the shear component of the tensile stress.

THE WEDGE DRAW TEST

Since conditions during deep-drawing are quite different from those produced in the ordinary tension test, it appeared desirable to find some form of simple test by which the deep-drawing qualities of a material could be evaluated. Mr. G. A. Brewer of the Production Research Group has developed the "wedge draw" test (originally devised by Sachs and others) to a point where it simulates very closely the actual drawing conditions.[17] The basis of this test is the segment of a disc such as that shown in Fig. 37. Such a segment is cut out as a coupon (Fig. 39) and inserted in a special test jig, shown in Fig. 40. In this fixture the coupon is restrained by plates arranged so as to reproduce the restraint that occurs in the deep-drawing process. The coupon is placed in the jig and then pulled out. The extent to which it is pulled out and stretched before breaking is a direct measure of the extent of deep-drawing that the material will withstand. This limit may be expressed as the ratio of R (radius of original blank) to r (radius of cup after drawing) as shown in Fig. 36. Fig. 41 shows some typical coupons before and after being tested in this manner. (Note diagonal shear failure in outer portion of specimen 6.) Correlation of limiting ratios with those found in drawing cylindric cups is very good.

Fig. 42 shows how, by slightly modifying the wedge test coupon, this coupon may be subjected to almost pure shear with resulting high elongations.

Fig. 43 shows how the grain structure (after recrystallizing heat-treatment) was progressively refined

by cold-working sustained as the coupon was drawn through the die previous to heat-treatment. This indicates the marked effect of cold-working, which is greatest at the outer end of the coupon.

Further discussion of this interesting test is beyond the scope of this paper. A complete description[17] is available, however, and will be published in the near future.

Other Forming Processes

The foregoing discussion includes most of the forming processes that lend themselves to analytic study. Other well-known methods that may be mentioned briefly are: drop-hammer, rolls, drawbench, and dimpling. The main difficulty in drop-hammer work comes from wrinkling, which is caused by the absence of a draw ring such as described under deep-drawing. Once wrinkles have started it is difficult to eliminate them. Although the hammering action obtained on the drop-hammer does eliminate some wrinkles, it is nearly always necessary to work out the wrinkles by hand-hammering between successive forming operations. This work-hardens the material and seriously limits the subsequent forming.

Rolls are used primarily to form thin sheet, which would have excessive spring-back if bent to a form block. The rolls apply a local bending strain which is great enough to cause permanent set. This local condition is applied progressively as the sheet passes through the rolls. The spring-back analysis may be used to determine the approximate local radius of curvature that must be produced by the rolls.

The *drawbench*, as applied to sheet metal work, is usually used to form long stringers from strip stock. The operation is a combination of stretching and local bending. Rolls are often used as dies. The stretching principle is effectively used to eliminate wrinkling that occurs during heat-treatment.

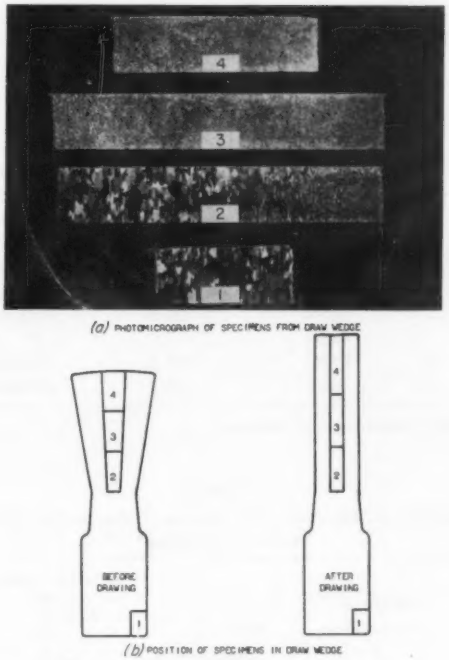

Fig. 43. Refinement of grain structure after heat-treatment due to wedge draw test before heat-treatment.[17]

Dimpling may be analyzed by calculating the amount of stretch required. Cracking of dimples may be predicted and explained on the basis of local elongations. The process of forging the dimple to prevent cracking is a good example of increasing the permissible elongation by the application of combined tension and compression, as described in Part 1. A complete knowledge of material properties under combined stress conditions would enable such problems to be solved analytically, thus eliminating much trial-and-error experimentation.

Part 3
Comparison of Materials

One of the objectives in the forming research program has been to develop suitable criteria by which various materials may be compared as to formability. From the foregoing discussion it is obvious that there is no single characteristic that may be used to cover all forming problems. In addition to the standard characteristics, certain criteria for each general class of forming have therefore been established.[21] These are as follows: local bending and stretching—elongation (per cent), in $^1/_4$ in.; stretching (long lengths)—elongation (per cent), in 40 to 50 in.; shrinking—E'/F_{cy} = tangent modulus/yield stress; deep drawing—maximum R/r ratio (see Fig. 36).

A comparison of several materials for local bending is given in Table 1. These values indicate relative formability for such operations as bending over a small radius, dimpling, joggling, etc.

Table 1
Formability of Materials: Local Bending and Stretching

Order of Formability	Material (Aluminum Alloy)	Elongation* at Failure in $^1/_4$ In., %
1	52SO	37
2	53SW	30
	61SW	
	24SO	
3	24ST	23
4	52S-$^1/_2$H	20

* Minimum observed values.

TABLE 2

Formability of Materials: Stretching of Long Sheets

Order	Material	Elongation* at Failure in 50 In., %	Edge Condition
1	52SO	13.0	Sheared
2	53SW	11.0	Sheared
3	24SO	9.5	Sheared
4	61SW	8.0	Sheared
5	24ST	4.5	Sheared
6	52S-$^1/_2$H	3.0	Sheared
1	61SW	17.5	Polished
2	53SW	16.5	Polished
	52SO		
3	24SO	10.5	Polished
4	24ST	8.5	Polished

* Minimum observed values.

TABLE 3

Formability of Materials: Compression (Unsupported Shrinking) without Wrinkling*

Order of Formability	Material	Relative Index $S = \frac{E'}{F_{cy}}$
1	24SO	13.5
2	52SO	11.4
3	61SW	7.1
4	53SW	5.5
5	24ST	2.91

* Applies to parts requiring a maximum "shrink" of 4 per cent.

TABLE 4

Formability of Materials: Drawing (with Draw Ring)

Order of Formability	Material	Gauge	Limit of Drawing R/r
1	24SO Alclad	0.051	1.98
		0.032	1.85
2	24SW Alclad	0.032	1.81
	52SO	0.040	1.81–1.9
	2SO	0.040	1.81
	3SO	0.040	1.81
3	3S-$^1/_2$H	0.040	1.73
4	61SW	0.032	1.7
	53SW	0.032	1.7
	52S-$^1/_2$H	0.040	1.65
5	24ST Alclad	0.032	1.45

Table 2 indicates relative formability for stretching operations involving a considerable length of material. Note that the elongation is affected by the edge condition. Polished (or even routed or filed) edges permit an appreciably greater elongation without failure than edges left in the rough as-sheared condition. Stress concentrations resulting from notch effect appear to cause early tensile failure with sheared edges. The higher the yield stress, the more sensitive the metal is to a rough edge.

An index of shrink forming has been discussed under the bending of curved flanges. Derivation of this index (denoted by S) is discussed in the Appendix. Table 3 lists various metals in order of their shrinkability, as measured by this approximate factor.

The information available from preliminary wedge draw tests is presented in Table 4, as an index of deep-drawing properties measured by the factor R/r.

The values in Table 4 apply to coarse grained 24SO Alclad. It has been found that for fine grained 24SO Alclad the limiting R/r ratios are less favorable, being of the order 1.79–1.85 (taking into account allowance for trimming off ears, which tend to form with fine grained material due to the directional properties thereof).

The data presented in the accompanying tables are for aluminum alloys, but it is hoped that similar data will eventually be available for all sheet material likely to be used in production. It is particularly important to know the important forming qualities of substitute materials, since such knowledge may eliminate possible production difficulties and will indicate the extent to which any material may be used for fabrication.

Uniformity of quality is another important factor in production work. Obviously, any large variation in forming properties may prove to be serious in production. It is not essential that materials be nearly uniform in quality, but it is necessary to know the extent of the variations to be expected. With this knowledge, proper allowances, tolerances, and margins of safety can be established; without it, full use of the formability of the material cannot safely be made. It is therefore recommended that, in addition to suitable forming criteria, typical frequency distribution diagrams be obtained from statistical measurements. Such information is difficult to obtain at present and represents one of the important unknowns in the solution of forming problems.

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Appendix

Derivation of Index for Shrink Formability

In forming a shrink flange such as shown in Fig. 44, the critical buckling stress F_{cr} for each element aa' must be greater than the compressive stress existing in that element if wrinkling is to be avoided. The greatest

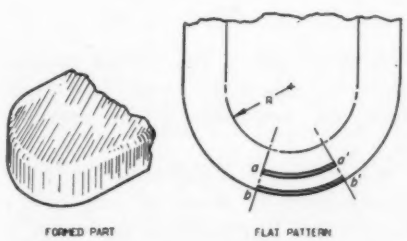

Fig. 44. Shrink flange showing compression required.

compressive stress—hence, the critical condition—exists in the outermost element bb'. This may be denoted by $(f_c)_{max.}$. Then the ratio of F_{cr} to $(f_c)_{max.}$ is an index of the ability of the material to shrink without buckling.

$$S = \frac{F_{cr}}{(f_c)_{max.}} = \text{index of "shrink" formability} \quad (1)$$

For constant thickness and unsupported width,

$$F_{cr} = kE' \quad (2)$$

Where E' = tangent modulus at point being considered—in this case, $e_{max.}$ (Fig. 45).

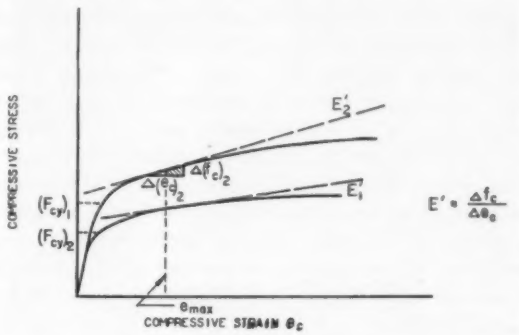

Fig. 45. Comparison of two metals with differing compressive stress-strain diagrams.

Substituting Eq. (2) in Eq. (1),

$$S = kE'/(f_c)_{max.} \quad (3)$$

Also, it may be assumed that for most of the aluminum alloys the general shape of the compressive stress-strain diagram is roughly similar and that for any given strain e_x the corresponding stress $(f_c)_x$ bears the same ratio to the yield stress F_{cy} for any material. Thus,

$$(f_c)_x/F_{cy} \cong k' \text{ (for any given strain } e_x) \quad (4)$$

For a given part, the maximum strain $e_{max.}$ is determined by the geometry of the part and will be the same regardless of the material used. If this value $e_{max.}$ is used in place of e_x as a basis of comparison in Eq. (4), we have:

$$(f_c)_{max.}/F_{cy} \cong k' \quad (5)$$

Substituting Eq. (5) in Eq. (3):

$$S \cong \frac{kE'}{k'F_{cy}} \cong K\frac{E'}{F_{cy}} \cong K\frac{E'}{F_{ty}} \quad (6)$$

The tensile yield stress F_{ty} may be substituted for the compressive yield stress F_{cy}, as the two are approximately the same and the former is more easily determined.

By properly evaluating K in Eq. (6), the critical limit of shrink formability (for which $S = 1$) could be determined in any specific case. However, for present purposes only the relative formability of different materials is of interest, and this can be approximately evaluated by assigning K the arbitrary value 1, substituting physical values for E' and F_{ty}, and arranging the resulting values of S in order of relative magnitude, as is done in Table 3 of the main text.

Dynamic Balancing as Applied to Transport Aircraft Propellers

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THE PROBLEM OF OBTAINING dynamically balanced propellers is one that has been considered seriously only during the past few years. Previously, it had been felt that a propeller balanced statically before installation on an engine would provide satisfactory service. The fact that some propellers ran smoother than others, although both had been statically balanced, brought about an investigation of dynamic balancing problems. This paper will deal mostly with practical problems as encountered in routine airline propeller balancing, as well as propeller balancing research now being carried on.

Two methods of dynamic propeller balancing are in use by the airlines at the present time. The newer of the two is under development by the Hamilton Standard Propeller Company and gives promise of being more efficient than the other more universally used balancing method. Both systems, however, accomplish approximately the same result, and in consideration of the greater practical experience and the purpose of this paper the older method is used in all comparative analysis and practical examples.

The propeller balancing method as considered in this paper was originally applied to aircraft by Mr. Albert Gail‡ of United Air Lines. This system in its present state of development utilizes a portion of the aircraft which at some practical speed of the aircraft propeller will vibrate in resonance with any out-of-balance of the propeller. In order to apply this method of balancing to an airplane, a vibration survey of structural and nonstructural members of the aircraft must be made at various propeller speeds. With the Douglas DC-3 airplane powered by a Wright C9GB (GR1820—G-102) engine of 3:2 propeller reduction gear ratio, the control column vibrates as a vibrating reed in resonance with the propeller unbalance at a propeller speed of 1,150 r.p.m. An indicator is mounted on the control column in such a manner that the amplitude of vibration of the control column can be visually noted as shown in Figs. 1 and 2.

Fig. 1 shows the location and method of mounting the vibration indicator on the control column. The indicator is mounted so that its centerline passes through the fixed end of the control column. This type of mounting aids in the visual observations made in obtaining the amplitude of the control column vibration.

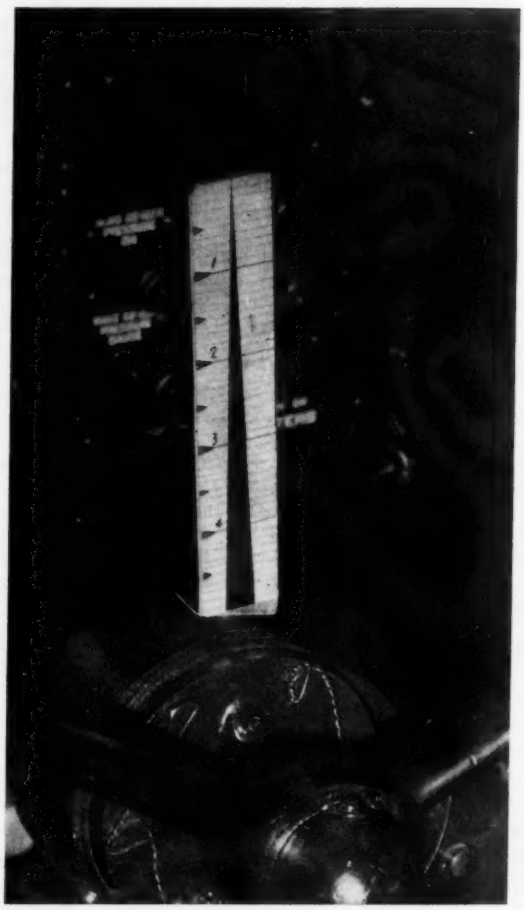

FIG. 1.

Fig. 2 is a drawing demonstrating the optical effect by which the indicator shows an amplitude reading. It can be seen that the motion of the wedge indicates the amplitude of vibration of the column.

In order to determine the direction and relative amount of propeller unbalance, a weight is applied to the propeller to create an out-of-balance of known magnitude in a known direction. This out-of-balance is applied to a propeller blade as shown in Fig. 3, and the propeller is then run at the control column resonant

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‡ Exhibit "C" Engineering & Maintence Memorandum #14, Air Transport Association of America.

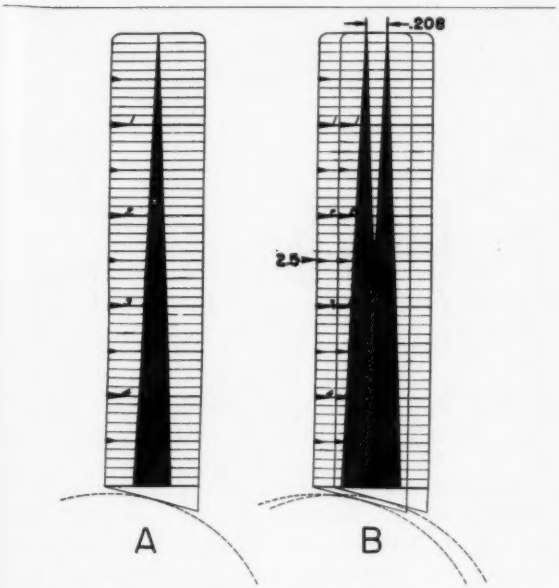

FIG. 2. Dynamic propeller balancing indicator. A—Appearance at rest. B—Appearance when vibrating. Example: Indicator slopes cross at 2.6. Slope of inclined lines cross at 15.04. Therefore, to get amplitude multiply reading by 0.08 ($2.6 \times 0.08 = 0.208$ = amplitude shown). Broken lines show position of head of control column.

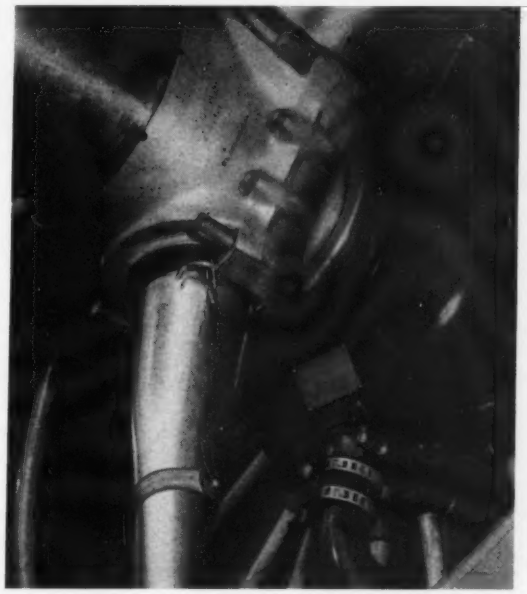

FIG. 3.

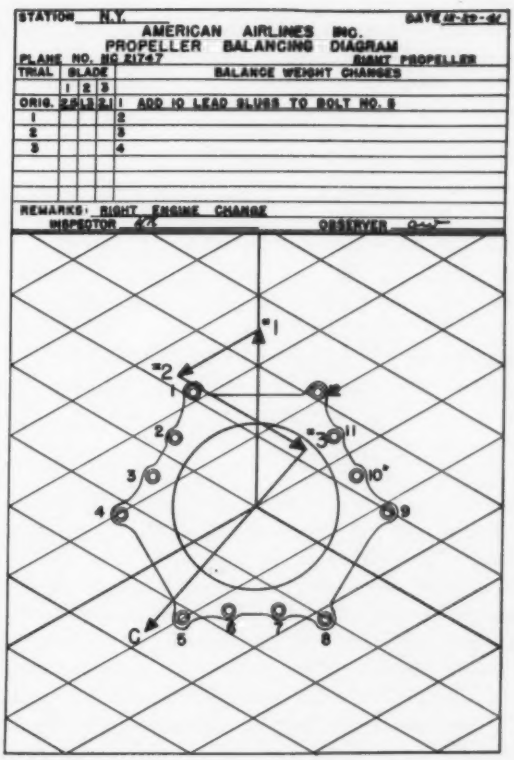

FIG. 4.

speed. The procedure is repeated for each propeller blade, and the results of these unbalance runs on the propeller blades are then plotted vectorially to give an approximate value for the original unbalance of the propeller (Fig. 4). The vectors 1, 2, and 3 of this diagram refer, respectively, to the unbalance of blades No. 1, No. 2, and No. 3. The vectors have direction and comparative magnitude (with other vectors). Vector C then indicates the direction in which corrective weight must be applied to the propeller. This vector solution is not rigorously correct in that it does not exactly account for the fact that each trial run unbalance is the result of both the original unbalance and the trial weight unbalance. The solution is nearly enough correct, however, that a satisfactory balance can be obtained with several trials. An exact vectorial solution has been used, but the results do not justify the added time and complexity of the system.

Differences in stiffness coefficients in airplane control columns, engine mounts, and aircraft structures must be calibrated to give magnitude, as well as direction, of the vectors. Such a calibration must be made on each airplane of a fleet in order to be applied in practical propeller balancing. The variations in each individual engine mounting at times of engine changes prevents a complete calibration.

Fig. 3 shows the special hollow propeller hub bolts in which the corrective weights, in the form of lead slugs, are added. Weight of an amount determined by previous calibration is placed in the desired direction. Another pattern is then run to determine exactly what change has been made in the balance condition. Other corrective weights are applied and the procedure repeated until a satisfactory balance has been obtained. It is considered with the Douglas DC-3, powered by Wright C9GB (G-102) engines with a 3:2 gear ratio,

that for satisfactory balance it is necessary to correct until all three blade vectors are read as equal.

The practical benefit of dynamic balancing of airplane propellers may be realized by considering a recent test installation. Two propellers were statically balanced to within 2.45 in.oz. and installed on a Douglas DC-3 airplane. Investigation by dynamic propeller balancing showed that the left propeller was 42 indicated in. oz. out of balance and the right propeller was 15 indicated in. oz. out of balance. Applications of the practical propeller balancing method reduced the out-of-balance of both propellers to 1.0 indicated in. oz. The 1.0 indicated in. oz. of out-of-balance was determined by analysis with the Hamilton Standard Electrical Field Balancing Equipment. The airplane was flown before and after the dynamic propeller balancing and a sound level analysis was made on both flights. A structural vibration analysis was also made, but the results have not yet been correlated for presentation and must be augmented by further research before final presentation. For the sound level analysis, readings were taken in the passenger cabin and in the cockpit. As can be seen from Fig. 5, the cabin sound level is not materially affected by the propeller balancing—that is, the net increase in propeller vibration components is approximately the same as the net decrease.

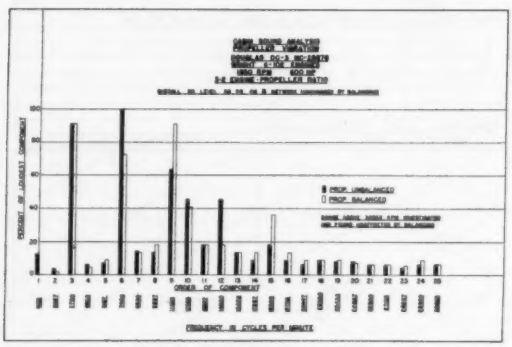

FIG. 5.

Considering the cockpit analysis of Fig. 6, however, we find a material change made by the propeller balancing. The balancing considerably decreased the intensity of the lower components of the propeller vibration sound, while the higher components were not materially affected. At the same time, the reduction in visible vibration of the control pedestals and fittings in the cockpit in the lower frequencies was appreciable.

In considering the factors that affect propeller balance, it is found that many routine engine maintenance procedures will affect balance. Several routine maintenance procedures were investigated. First, a propeller dome was removed—a normal procedure during investigation of some types of operation complaints. After the dome had been replaced, using the same indexing with respect to the propeller hub as when removed, it was found that the balance had been

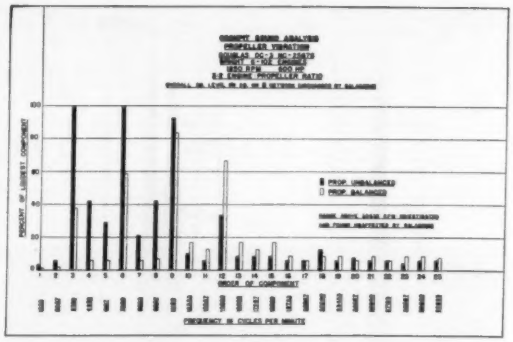

FIG. 6.

changed 12 indicated in. oz. Another dome removal was tried on a different propeller, with the dome being rotated 120 deg. before replacement. After reinstallation of this dome, it was found that the balance condition for this propeller had been changed 7.5 indicated in. oz. Finally, two balanced propellers on an airplane were interchanged and the propellers were then found to be 16.7 and 8.7 indicated in. oz. out of balance, respectively.

The foregoing investigations indicate that the propeller-engine combination and irregularities of mounting all enter into the balance problem. It is therefore evident that the final balancing of the propeller should be made with the propeller installed in position on the engine on which it is to operate. Research is now being conducted to see if this balance can be obtained on the test stand as a final portion of test stand procedures before the power plant assembly is placed in the airplane. It is evident that since routine maintenance items will affect propeller balance a more exact reinstallation of propellers and parts must be made, or else there must be a propeller rebalance any time a propeller dome or propeller is removed. It is anticipated, however, that little improvement can be made in refinement of reinstallation procedures for propeller domes and propellers of the present-day design. For the present, therefore, a propeller balancing method that can be applied quickly and easily after certain maintenance operations must be relied upon.

Since it is known that present-day routine maintenance does affect the dynamic balance of propellers and that it is necessary that the engine and propeller be balanced together for smooth operation, the logical approach to the problem is found at the original design of the propeller and propeller-engine mountings. Every new engine and propeller combination should be so designed that all of the mounting dimensions affecting balance can be held to the closest possible tolerances. At the same time, a more thorough investigation on dynamic power plant mounting problems must be made for every new propeller-engine combination. If this procedure can be followed in future design work, the necessity for balancing the propeller and engine combination will be greatly reduced, if not eliminated.

Optimum Performance of Pendulum-Type Torsional Vibration Absorber

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SUMMARY

The crankshaft-propeller system of a single row radial aircraft engine may be considered as a single-degree of freedom system with one natural frequency. After an absorber device is incorporated, it becomes a system of two degrees of freedom with two natural frequencies. It appears that the introduction of the absorber would give worse results than if the device were not incorporated, since it splits one natural frequency to two natural frequencies instead of removing the resonance. The purpose of this article is to investigate the two natural frequencies of the resulting system and the factors that control them. It was found that one of the natural frequencies is insignificant and, by properly adjusting the moment of inertia of the pendulum, the other natural frequency can be kept well away from resonance with the forced frequencies of the harmful orders of torque harmonics.

SYMBOLS

I_1, I_2	= moments of inertia of the propeller and the moving parts at the crank throw, respectively, the latter being approximately equal to moment of inertia of rotating mass + half of reciprocating mass × square of crank radius
I	= $I_1I_2/(I_1 + I_2)$ = moment of inertia of the equivalent moving parts at the crank throw with the propeller end fixed
I_0	= $I - m(R + L)^2$ = same, excluding that of the pendulum
m	= mass of the pendulum
R, L	= distances, as defined in Fig. 3
k	= radius of gyration of the pendulum about its center gravity
K	= torsional stiffness of the shaft
Ω_n	= $\sqrt{K/I}$ = natural frequency of the original system (without pendulum device)
ω_n	= natural frequencies of the resulting system (with pendulum device)
ω_{n1}, ω_{n2}	= two values of ω_n, as defined by Eqs. (21) and (22)
p	= frequency of the forcing torque
Ω	= 2π × r.p.s. of the engine
n	= p/Ω = nth order of torque harmonic
f	= Ω/Ω_n = rotating frequency ratio
p/Ω_n	= forced frequency ratio
g	= ω_n/Ω_n = natural frequency ratio
g_1, g_2	= two values of g, as defined by Eqs. (19) and (20)
μ	= $m(R + L)^2/I$ = ratio of the moment of inertia of the pendulum to the moment of inertia of the equivalent parts at the crank throw
M_0, ψ	= amplitude and phase angle of the forcing torque, respectively
θ, ϕ	= geometric angles, as defined in Fig. 4

IN THE CASE OF a radial aircraft engine with single throw crankshaft, the crankshaft-propeller system can be approximately represented by a system as shown in Fig. 1, where A represents a circular disc with its moment of inertia I_1 equal to the moment of inertia of the propeller, B is another disc with its moment of inertia I_2 equal to the moment of inertia of the moving parts at the crank throw, and S is a shaft of torsional stiffness K having its length equal to the length of the crankshaft from the propeller to the center of the crank throw. Because this system is free at both ends, it will vibrate with a node at some point O. The natural frequency of the system is given by

$$\Omega_n = \sqrt{K(I_1 + I_2)/I_1I_2} \tag{1}$$

Since only relative motion is involved, this system can be reduced to another system with propeller end fixed and with the same frequency, as shown in Fig. 2, where the two discs A and B of Fig. 1 have been replaced by another disc C having its moment of inertia equal to

$$I = I_1I_2/(I_1 + I_2) \tag{2}$$

Then Eq. (1) reduces to

$$\Omega_n = \sqrt{K/I} \tag{3}$$

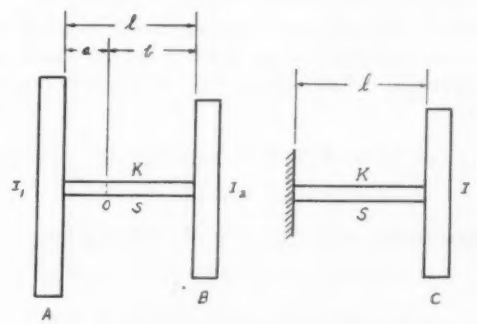

FIG. 1. FIG 2.

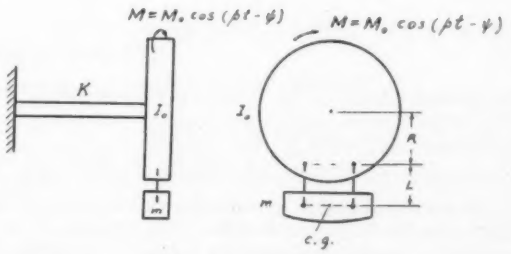

FIG. 3.

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Tuning and Amplitude Equations

Fig. 3 shows the modified system of Fig. 2 when the vibration absorber device is incorporated. In Fig. 3, I_0 is the moment of inertia of the moving parts at the crank throw, excluding that of the pendulum*—that is, $I_0 = I - m(R + L)^2$.

Let the whole system rotate about the axis of the shaft with an average angular speed Ω, on which is superposed a forcing torque $M = M_0 \cos(pt - \psi)$. This torque causes the disc I_0 to rotate at an angle θ (Fig. 4) with respect to the other fixed end and the pendulum to swing at an angle ϕ from its mean position. By the geometry of Fig. 4, $\sin\beta = (R/OB)\sin\phi$.

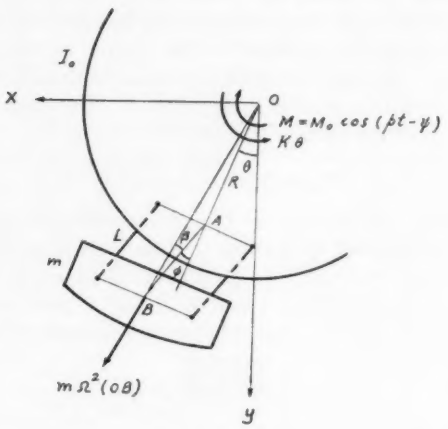

Fig. 4.

Considering the relative motion of the system with respect to the uniformly rotating coordinate system Ω, the linear velocity v of the point B can be found as in the following. Referring to Fig. 4, the coordinates of B are

$$x = R\sin\theta + L\sin(\theta + \phi)$$

$$y = R\cos\theta + L\cos(\theta + \phi)$$

Differentiating with respect to the time t gives

$$\dot{x} = R\dot{\theta}\cos\theta + L(\dot{\theta} + \dot{\phi})\cos(\theta + \phi)$$

$$\dot{y} = -R\dot{\theta}\sin\theta - L(\dot{\theta} + \dot{\phi})\sin(\theta + \phi)$$

Hence

$$v^2 = \dot{x}^2 + \dot{y}^2 = R^2\dot{\theta}^2 + L^2(\dot{\theta} + \dot{\phi})^2 + 2RL\dot{\theta}(\dot{\theta} + \dot{\phi})\cos\phi$$

The kinetic energy of the system is then equal to

$$T = \tfrac{1}{2}I_0\dot{\theta}^2 + \tfrac{1}{2}(mk^2)\dot{\theta}^2 + \tfrac{1}{2}m[R^2\dot{\theta}^2 + L^2(\dot{\theta} + \dot{\phi})^2 + 2RL\dot{\theta}(\dot{\theta} + \dot{\phi})\cos\phi] \quad (4)$$

* Because the pendulum is originally a part of balancing mass rigidly attached at the crank throw. The introduction of the pendulum is accomplished simply by pivoting a part of the balancing mass and permitting it to swing, no additional mass being involved. Therefore, the relationship $I = I_0 + m(R + L)^2$ holds.

Lagrange's equation of motion for this case is of the form:

$$(d/dt)(\partial T/\partial\dot{q}_i) - (\partial T/\partial q_i) = Q_i$$

where T is kinetic energy of the system and Q_i is the generalized force corresponding to the generalized coordinate q_i. The generalized force with respect to θ is

$$Q_\theta = M_0\cos(pt - \psi) - K\theta \quad (5)$$

The generalized force with respect to ϕ is

$$Q_\phi = -m\Omega^2(OB)L\sin\beta = -m\Omega^2RL\sin\phi \quad (6)$$

Assuming small oscillations, i.e., $\cos\phi = 1$, $\sin\phi = \phi$, Eqs. (4) and (6) become, respectively,

$$T = \tfrac{1}{2}(I_0 + mk^2)\dot{\theta}^2 + \tfrac{1}{2}m[(R + L)\dot{\theta} + L\dot{\phi}]^2 \quad (7)$$

$$Q_\phi = -M\Omega^2RL\phi \quad (8)$$

Applying Lagrange's equation for the coordinates θ and ϕ leads to the following two equations of motion:

$$(I_0 + mk^2)\ddot{\theta} + m(R + L)[(R + L)\ddot{\theta} + L\ddot{\phi}] = M_0\cos(pt - \psi) - K\theta \quad (9)$$

$$(R + L)\ddot{\theta} + L\ddot{\phi} = -\Omega^2R\phi \quad (10)$$

Assuming no damping, the forced vibrations of the system will be of the form:

$$\theta = \theta_m\cos(pt - \psi)$$

$$\phi = \phi_m\cos(pt - \psi)$$

Substituting these in Eqs. (9) and (10) and solving for θ_m and ϕ_m, one obtains

$$\frac{\theta_m}{\theta_{st.}} = \frac{(\Omega^2R - p^2L)/p^2(R + L)}{\left[1 - \frac{p^2(I_0 + mk^2)}{K} - \frac{p^2m(R + L)^2}{K}\right]\left[\frac{\Omega^2R - p^2L}{p^2(R + L)}\right] - \frac{p^2m(R + L)L}{K}} \quad (11)$$

$$\frac{\phi_m}{\theta_{st.}} = \frac{1}{\left[1 - \frac{p^2(I_0 + mk^2)}{K} - \frac{p^2m(R + L)^2}{K}\right]\left[\frac{\Omega^2R - p^2L}{p^2(R + L)}\right] - \frac{p^2m(R + L)L}{K}} \quad (12)$$

where $\theta_{st.} = M_0/K$ = static deflection of I_0.

If I_0 does not vibrate, θ_m in Eq. (11) must be equal to zero—that is,

$$(\Omega^2R - p^2L)/p^2(R + L) = 0$$

Hence

$$p = \Omega\sqrt{R/L} \quad (13)$$

The amplitude of the pendulum is then obtained from Eq. (12):

$$\phi_m = -\frac{M_0}{p^2 m(R+L)L} \tag{14}$$

If it is desired to eliminate the nth order of torque harmonic, then $p = n\Omega$, and Eqs. (13) and (14) give, respectively, the following tuning and amplitude equations:

$$n = \sqrt{R/L} \tag{15}$$

$$\phi_m = -M_0/mn^2\Omega^2(R+L)L \tag{16}$$

Physical Significance of Eqs. (13) and (14)

For a simple pendulum swinging under gravitational field, if the amplitude is small, the frequency is $\sqrt{g/L}$; if under centrifugal field, the frequency then is $\Omega\sqrt{R/L}$. Hence Eq. (13) implies that the natural frequency of the pendulum should be tuned to be equal to the frequency of the forcing torque for the condition that $\theta_m = 0$.

Eq. (14) can, in view of Eq. (13), be written as

$$\phi_m = -M_0/m\Omega^2 R(R+L)$$

Transposing and multiplying both sides by $\cos(pt - \psi)$, there is obtained

$$-m\Omega^2 R(R+L)\,\phi_m \cos(pt-\psi) = M_0 \cos(pt-\psi)$$

or,

$$-m\Omega^2(R+L)R\phi = M$$

The left-hand side of the equation is the torque acting on I_0 which is produced by the centrifugal force (to be called the centrifugal torque). The negative sign signifies that the phase angle between the centrifugal torque and the forcing torque is 180 deg. Hence, the whole equation means that the centrifugal torque is at all instants equal and opposite to the forcing torque for the condition that $\theta_m = 0$.

Correction for C.G. Location

Eqs. (13) and (14) were derived for the case in which the center of gravity of the pendulum was at a distance of $R + L$ from the axis of the shaft. If the center of gravity were located at a distance S from the original position and in a direction away from the axis, then the pendulum would move as if it were pivoted at a distance of $R + S$, instead of R, from the axis of the shaft. In this case the R in all the above equations should be replaced by $R + S$.

Frequency Equation

Returning to Eqs. (11) and (12), it is seen that their denominators are equal and, if the denominators are set equal to zero, both θ_m and ϕ_m become infinite. The resulting equation is a quadratic equation in p^2 and gives two natural or resonant frequencies of the system. Thus, the introduction of the absorber does not remove the resonance but, instead, splits the original natural frequency into two natural frequencies.

Natural Frequencies of the Resulting System

The frequency equation is

$$\left[1 - \frac{p^2(I_0 + mk^2)}{K} - \frac{p^2 m(R+L)^2}{K}\right] \times \left[\frac{\Omega^2 R - p^2 L}{p^2(R+L)}\right] - \frac{p^2 m(R+L)L}{K} = 0 \tag{17}$$

To simplify this equation, assume that $mk^2 << I_0 + m(R+L)^2$; then Eq. (17) becomes

$$\left[1 - \frac{p^2 I}{K}\right]\left[\frac{\Omega^2 R - p^2 L}{p^2(R+L)}\right] - \frac{p^2 m(R+L)L}{K} = 0 \tag{18}$$

Making use of the relations,

$$n = \sqrt{R/L} \quad \text{and} \quad \Omega_n = \sqrt{K/I},$$

further letting,

$$f = \frac{\Omega}{\Omega_n}, \quad g = \frac{\omega_n}{\Omega_n}, \quad \text{and}$$

$$\mu = m(R+L)^2/I$$

and noting that at resonance, $\omega_n = p$, Eq. (18) reduces to

$$(1-\mu)g^4 - (1 + f^2n^2)g^2 + f^2n^2 = 0$$

or,

$$g^2 = \frac{1 + f^2n^2 \pm \sqrt{(1+f^2n^2)^2 - 4(1-\mu)(f^2n^2)}}{2(1-\mu)}$$

The two roots of g are

$$g_1 = \sqrt{\frac{1 + f^2n^2 + \sqrt{(1+f^2n^2)^2 - 4(1-\mu)(f^2n^2)}}{2(1-\mu)}} \tag{19}$$

$$g_2 = \sqrt{\frac{1 + f^2n^2 - \sqrt{(1+f^2n^2)^2 - 4(1-\mu)(f^2n^2)}}{2(1-\mu)}} \tag{20}$$

Hence, the natural frequencies of the resulting system are

$$\omega_{n1} = \Omega_n \sqrt{\frac{1 + f^2n^2 + \sqrt{(1+f^2n^2)^2 - 4(1-\mu)(f^2n^2)}}{2(1-\mu)}} \tag{21}$$

$$\Omega_{n2} = \Omega_n \sqrt{\frac{1 + f^2n^2 - \sqrt{(1+f^2n^2)^2 - 4(1-\mu)(f^2n^2)}}{2(1-\mu)}} \tag{22}$$

Optimum Performance of the Absorber

In the case of a single row radial aircraft engine, only major critical speeds are dangerous. The corresponding orders of torque harmonics can be expressed by

$$n = j \times \frac{\text{no. of cylinders}}{2}$$

where $j = 1, 2, 3 \ldots$. In general, only the first two of

the major critical orders are of importance and the higher orders may be neglected.

The problem now is to investigate for what value of μ, will the pendulum give the best performance. Take, for instance, a nine-cylinder engine with the pendulum tuned for $n = 4.5$. Substituting this value of n into Eqs. (19) and (20),

$$g_1 = \sqrt{\frac{1 + 20.25f^2 + \sqrt{(1 + 20.25f^2)^2 - 81(1 - \mu)f^2}}{2(1 - \mu)}} \tag{23}$$

$$g_2 = \sqrt{\frac{1 + 20.25f^2 - \sqrt{(1 + 20.25f^2)^2 - 81(1 - \mu)f^2}}{2(1 - \mu)}} \tag{24}$$

Fig. 5 shows the relation between the two natural frequency ratios g_1, g_2 and the forced frequency ratio p/Ω_n for various values of μ, plotted from Eqs. (23) and (24). It is seen that the g_2 curves are well below the 4.5th order line, and therefore the natural frequency g_2 is not of importance. The g_1 curves are practically straight lines except for very low values of f. This condition indicates that the system is constantly in resonance with an unimportant order of torque harmonic at all engine speeds for any particular value of μ. To have the best performance of the absorber it is necessary that the natural frequency g_1 of the system be kept not only well away from resonance with the 4.5th order of torque harmonic, but also well away from the 9th order. To this end, it is obvious from the g_1 curves that the pendulum absorber must be designed with a value of μ between 0.4 and 0.5.

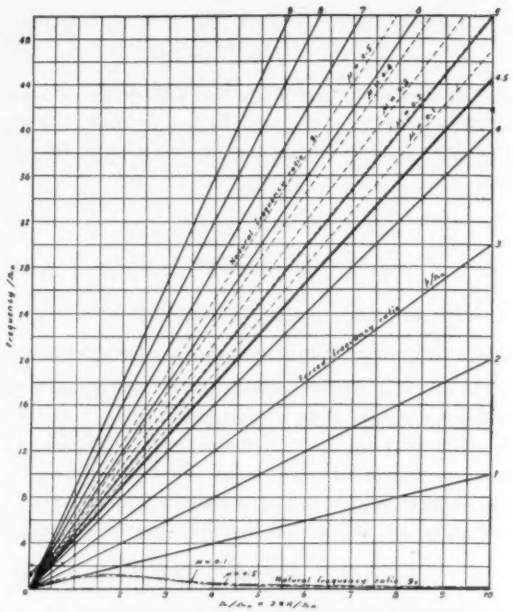

FIG. 5. Resonance curves of the crankshaft-propeller system of a nine-cylinder radial engine with absorber device.

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Use of Plastics and Allied Materials in Aircraft Construction

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MANY ENGINEERS look upon plastics and allied materials as entirely new to the aircraft industry, but such is not the case. Phenol fiber sheet and resin-bonded waterproof plywood have been used for years, and acrylic resin sheet has been in use for transparent enclosures for some time past, yet all come under the above category. The primary difference between the past and present uses of these materials is that they are now used in applications where structural loads are involved, while they were previously used only in non-stressed parts where special characteristics, such as transparency or insulating qualities, were of paramount importance. If these materials are classified according to their major characteristics, they fall into three categories—namely, those made with thermosetting resins, those made with thermoplastic resins, and those made with wood veneer. This classification also in a general way divides them according to their principal uses, thermosetting materials being used mostly in the production of relatively small structural parts, the thermoplastics being used mostly for their transparent properties, and the wood veneer materials being used mostly in relatively large structural parts and assemblies.

THERMOSETTING MATERIALS

The thermosetting materials, as their name implies, are materials manufactured with resins that upon complete polymerization become infusible solids that will not soften when heated. The polymerization of this class of resin can be attained in either of two ways: by the initial application of heat or by the use of a chemical accelerator. Polymerization by means of heat is the method in general use, with the exception of the cold-setting resin adhesives which are generally completely cured by the introduction of a chemical accelerator, although in certain instances a minor amount of heat is essential to complete cure.

A majority of the thermosetting materials used in aircraft is manufactured with the phenol formaldehyde resins, although urea formaldehyde and melamine resins are used in certain instances. The melamine resins are relatively new and are at present used only as thermosetting surface coatings where their permanent transparency is a distinct advantage. The major use of the urea formaldehyde resins in aircraft is as adhesives, but they are also used in applications where a thermosetting material is needed and where light colors are essential, since the phenol formaldehyde resin has an inherent mahogany brown color that prevents its use in such applications.

Large quantities of low impact phenol formaldehyde materials are used commercially in the various non-defense industries, but this class of material has only a limited application to aircraft, since its low impact strength precludes its use in all applications where there is the slightest danger of accidental damage. It is therefore relegated largely to the manufacture of control knobs and like parts. The high impact phenol formaldehyde materials have, however, mechanical properties that justify their extensive use in aircraft where the structural loads are not excessive. Such applications as radio masts, control quadrants, brackets in the bomb release system, equipment support brackets, and housings are well within the design limits of this material. Although this material might possibly be utilized for the manufacture of primary structural units, such as control columns and highly stressed brackets in the primary control systems, it is not felt that our design knowledge has progressed to the point where such applications could be safely undertaken without extensive design research and static testing.

Three methods are used for manufacturing parts from this material: they may be machined from sheet, tube, or bar stock, they may be molded from laminated material, and they may be molded from high impact molding material. The first method has been used for years and needs no discussion. The second method consists of impregnating sheets of cloth or paper with the uncured resin and forming and curing the desired number of layers of this material to the required shape in heated steel dies. Since the cloth or paper sheets used can only be formed or stretched to a reasonable degree without tearing, this process is limited to relatively simple shapes, such as channels, angles, and shallow pans. The third method consists of impregnating macerated or shredded fabric with the uncured resin, then pouring the impregnated material into the cavity of a heated steel die and curing and molding to the desired shape under high pressure.

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Thermoplastic Materials

The thermoplastic materials consist of those fully polymerized synthetic resin materials that will soften and become plastic upon application of heat. Under this classification fall the acrylics, notably Plexiglas and Lucite, the cellulose acetates, polystyrene, the vinyls, and others too numerous to mention.

At the present time the acrylics are by far the most important from an aircraft standpoint, although there is a possibility that in the near future some of the cellulose acetates and vinyls may also be used to an appreciable extent. The greatest use for these materials is in transparent enclosures where their clarity, combined with light weight, is of sufficient importance to offset their low scratch resistance and variation in strength with temperature. They can also be used to a limited degree in internal nonstressed parts, such as control knobs, instrument dial faces and placards, where their high rate of change of strength with temperature is of little importance. They should not, however, be used in any stressed application unless a thorough analysis has been made of the temperature effects. In this regard the effects of the radiant heat of the sun should be carefully considered and taken into account. One of the principal reasons that these materials have proved successful in transparent enclosures is that because of their high light transmitting efficiency they do not absorb the radiant heat of the sun to the same extent as dark colored or opaque materials and, therefore, attain a temperature only slightly above that of the ambient air. However, if the thermoplastic materials are used in opaque form or are mounted in intimate contact with opaque surfaces, their temperature will rise appreciably because of the effects of radiant heat, and this rise in temperature will be accompanied by a marked lowering in some of their physical properties and in their resistance to cold flow.

Two methods are used for manufacturing parts from these materials: (1) Sheet stock is heated until soft and stretched over forms of the desired shape or is pressed in the desired shape in heated dies, and (2) granulated material is molded to the desired shape in heated dies of either the compression or injection type. In either process the material must be cooled before being removed from the form or die in order to prevent distortion while the piece is being stripped.

Wood Veneer Materials

Wood veneer materials are normally supplied in the form of either flat or molded plywood in which the grain of adjacent plies is crossed or in the form of laminated wood in which the grain of all plies is parallel. The material may be supplied either impregnated or unimpregnated, but up to the present time almost all the material is of the unimpregnated type, the impregnated wood veneers being limited mainly to propeller blanks. The unimpregnated wood veneer materials are generally bonded with a thermosetting synthetic resin adhesive, although thermoplastic adhesives have been used to some extent. For maximum durability and resistance to moisture, hot-setting adhesives of the thermosetting type are preferred. Wood veneer materials made with the cold-setting thermosetting adhesives are believed to be slightly inferior under extreme conditions of moisture and heat, but within the normal aircraft operating range it is felt that this inferiority will not be evident. The best combination of serviceability and manufacturing facility can be attained by using the hot-setting thermosetting adhesives for the manufacture of the plywood sheets and the cold-setting thermosetting materials for the manufacture of the heavy frame members and for assembly.

When the hot-setting type of adhesives are used in the manufacture of heavy members there is always the danger of damaging the wood by excessive heat, since the curing time is a direct function of the thickness of the member. Serviceability of the thermoplastic adhesives in aircraft construction has yet to be conclusively proved, since there is still considerable question regarding the effect of cold flow of the adhesive at the maximum temperatures which may be experienced in tropical service. Existing information on this subject is contradictory, and it is therefore recommended that when such thermoplastic adhesives are used extreme care be exercised to insure that the glue lines are not highly stressed. Plywood made with such adhesives will probably, in most cases, be satisfactory, since the stresses in plywood glue lines are usually low, but in framing joints this is not usually the case. The thermosetting adhesives available at present are of the phenol formaldehyde and urea formaldehyde types and are offered in both cold- and hot-setting varieties. The thermoplastic adhesives in use at present are usually based on the vinyl resins or cellulosic compounds. Casein, blood albumen, and other protein plastics should, in general, be avoided as they are subject to bacteria and fungus attack which cannot be conveniently detected until failure occurs. Such attack of the protein plastics is prevalent in hot humid climates as would normally be experienced when operating in the tropics.

Impregnated wood veneer materials for general use in aircraft construction are just now becoming available, but their utilization and further development should be sponsored to the maximum possible degree since they are much stronger and stiffer than the ordinary laminated thermosetting plastics and can be molded at appreciably lower pressures. The Forest Products Laboratory research on these materials has indicated that by proper impregnation the ultimate swelling due to moisture absorption can be reduced to somewhat less than one-third the swelling of normal unimpregnated wood and that the rate of moisture absorption and swelling can be reduced many fold. Their research also indicates that this reduction in ultimate moisture

absorption and swelling is practically independent of the density of the resulting material; for example, starting with spruce veneer, impregnated materials can be produced of any density from 0.6 to 1.5. The physical properties of these materials will, in general, vary almost directly with the density, although the increase in certain factors is, of course, not quite so rapid. Laboratory samples of material having 1.4 density have indicated tensile strengths as high as 50,000 lbs. per sq.in., compression strengths are in the neighborhood of 35,000 lbs. per sq.in., and moduli of elasticity are in the neighborhood of 4,000,000 lbs. per sq.in.

Design Considerations Affecting Utilization

If the foregoing materials are to be successfully utilized, it is essential that the basic design of the parts be established by engineers who are fully cognizant of the aircraft manufacturing and operating problems, of the plastic manufacturers problems, and of the physical properties of the material under consideration. Such design control can be accomplished in any of three ways: (1) The design can be turned over entirely to the plastic manufacturer for development; (2) the design can be handled by the aircraft manufacturer working in cooperation with a particular plastic manufacturer; and (3) the design can be handled completely by the aircraft manufacturer.

The first method does not offer much hope of success, since few, if any, of the plastic manufacturers employ engineers who are sufficiently skilled in the manufacturing and operating problems of aircraft to produce a part entirely satisfactory from all standpoints. The second method has the disadvantage that the aircraft company must initially select a particular plastics' manufacturer with whom to work on each particular application; this eliminates the advantages of competitive bidding and oftentimes the plastic manufacturer selected is not the best source for the part under consideration. Also, in this method there is a tendency to design the part around the existing equipment of that particular manufacturer, even though this may make for an inferior design. The third method appears to have the fewest drawbacks, in that a skilled aircraft engineer can, in a reasonable length of time, become familiar with the general problems of the plastic manufacturers and can then produce a design that closely meets the requirements of everybody concerned. The design can then be sent out for competitive bidding and the final details worked out with the company selected to manufacture the part.

Experience has shown that the first and second methods are much more time-consuming than the third, since much more liaison is required between the aircraft manufacturer and the plastic manufacturer. When the third method is used, design differences can generally be cleared up in a single conference and on a majority of parts no conference at all is required. This procedure also makes for a more equitable distribution of responsibility, for the aircraft manufacturer is then responsible for the suitability of the application and its proper functioning and the plastic manufacturer is responsible for the integrity of the part itself from both a dimensional and materials standpoint.

Aircraft Factors Affecting Design

In order for plastics and allied materials to be utilized successfully in the manufacture of aircraft parts, several factors that are not normally considered in metal construction must be taken into account.

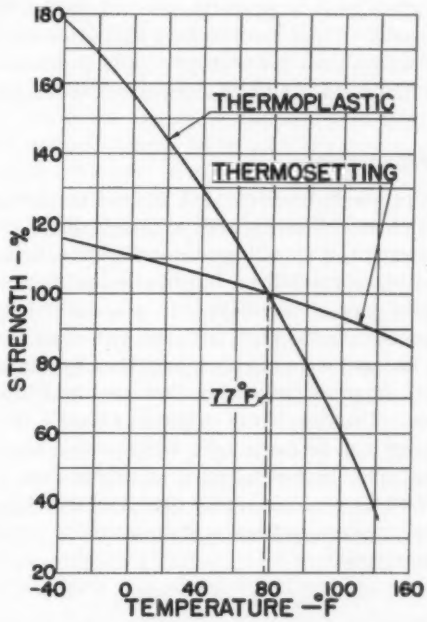

Fig. 1. Strength vs. temperature for thermosetting and thermoplastic materials.

Temperature

The most important of these factors is the effect of the operating temperature range. For a majority of aircraft parts this range is from $-70°$ to $+160°$F., the lower temperature limit being that which may be obtained in the arctic or in the stratosphere and the upper temperature limit representing temperature that can be obtained in the tropics, including the effect of the radiant heat of the sun. This upper limit is not considered to be excessive, as 190°F. has been obtained in tests of road surface temperatures in southern California. The relative effect of this temperature range on the tensile, compressive, and flexural strengths of the thermosetting and thermoplastic materials is shown in Fig. 1. The effect of temperature on the unimpregnated wood veneer materials is slight, provided no change in moisture content occurs. It will be noticed from Fig. 1 that the thermoplastic materials have, in general, a much greater change in strength with tempera-

ture, but this is only part of the story, as they also have a much greater tendency to cold flow at the higher temperature limit, and, therefore, the allowable stress at high temperature for the thermoplastics must be reduced even further than shown on Fig. 1.

Impact Strength

The second most important factor is the influence of the low impact strength of these materials on the possibility of accidental damage during installation and service. Although the thermosetting and thermoplastic materials are greatly inferior to aluminum alloy in impact strength, this does not indicate that they are inferior when used in properly selected and designed aircraft parts. Their lower impact strength is, in most cases, offset by their lower density, which permits the use of sections at least twice as thick when designed in plastics.

Weight Requirements

Many people have advocated the use of plastics on the basis of weight saving and, although this is true to a certain extent, it should not be carried to extremes or the resulting parts will be too fragile and flexible for satisfactory service operation. In general, it is better to design the plastic part for a weight equivalent to that of its metal counterpart and thereby avoid the possibility of producing parts that are unsatisfactory in service. Usually, if the draftsman is told that the plastic part is to be made for a weight equal to, or less than, the equivalent metal part, he will produce a unit that is 5 to 10 per cent under the allowed weight, yet perfectly satisfactory from a manufacturing and operating standpoint.

Resistance to Wear

The problem of wear on plastic parts can usually be controlled through proper use of inserts. In deciding where to put inserts, the designer should take into account the expected service life, the type of wear involved, the bearing stresses developed, and the type of lubrication which will be supplied. Wherever screw threads are called for it is advisable to use inserts so that the threads will be entirely of metal, as plastic threads, particularly in the smaller sizes, are subject to chipping and when tightened have a tendency to seize on the screw so that the plastic thread is destroyed when the screw is removed. It is, however, permissible to use plastic threads in certain instances where the thread is merely an assembly feature to simplify mold design, but in such instances an adhesive is usually applied to the threads so that the pieces cannot again be disassembled.

Plastics should be used for the bearings of rotating shafts only when the bearing stresses and speeds are low and when friction is not an important consideration; otherwise it is advisable to use metallic inserts in such positions. In this regard Oilite bushings may be used as molded-in inserts, provided they are not filled with oil at the time of molding. The porosity of this type of bearing provides an excellent bond between the insert and the plastic and at the same time the bushing only fills up with resin for a depth of about twelve-thousandths from the surface. After molding, the part is then submerged in hot oil to impregnate the bushings. However, when such porous bushings are molded into the plastic part, it is essential that the mold pins over which the bushings are placed in the mold be close fits for the bushings, otherwise there is danger of the resin running in between the bushing and the mold pin and sealing the operating surface of the bushing.

In certain specific applications the thermosetting plastics have proved themselves superior to the metals as bearing materials. This is evidenced by the use of fabric-base phenolic laminated timing gears in the majority of automobile engines and by the use of fabric-base phenolic laminated bearings in heavy rolling mill equipment. In the rolling mill application the bearings are lubricated with water, while in the timing gear application they are, of course, lubricated with oil in the normal manner. When quietness of operation under vibrating conditions is desired, thermosetting plastics offer considerable value as bearing materials, provided due precaution is taken to insure that the allowable loads are not exceeded and that adequate lubrication is provided. The afore-mentioned timing gear application comes in this category.

Coefficient of Expansion

When relatively large plastic parts are rigidly mounted to metal supporting structures, due account should be taken of the differential coefficient of temperature expansion of the two materials. It should be remembered that the parts may be assembled at 90°F. and then operated at −70°F., thereby producing stresses and strains equivalent to the differential coefficient of expansion times the temperature difference of 160°F. Such stresses and strains may be an appreciable percentage of the allowable stress and, therefore, care must be exercised to insure that the sum of these stresses and the normal design stresses do not exceed the allowable. Oftentimes these temperature stresses may be eliminated or materially reduced by utilizing a mounting method that permits relative motion between the plastic part and its metallic supporting structure. This relative expansion problem is not usually important on small plastic parts, such as brackets and fittings, but it definitely must be taken into account when designing large transparent enclosures, such as turrets, bomber noses, and pilots' enclosures. It is also more important when dealing with the thermoplastic materials than with the thermosetting ones, since the thermoplastics have a greater differential coefficient of expansion.

Moisture Absorption

The thermosettings, the thermoplastics, and wood veneer materials all absorb moisture, but to varying degrees. The thermosetting and thermoplastic materials are only slightly sensitive to moisture, and this factor may be ignored except in severe electrical applications or where dimensional tolerances are exceptionally close. Such is not the case when using the unimpregnated wood veneer materials whose strength and dimensions vary noticeably with the moisture content. The effect of moisture on the unimpregnated wood veneer materials may be greatly reduced by the proper application of the high-grade synthetic resin finishes which have been developed over the last three or four years, but it should be borne in mind that no finish is entirely impervious to moisture and, therefore, that even the best finishes merely reduce the rate of moisture absorption. Although these modern finishes offer adequate protection under fluctuating atmospheric conditions, they do not offer moisture protection when the atmospheric conditions maintain over long periods of time. For instance, if the finish was applied to the wood veneer piece in the northern part of the country under atmospheric conditions that would produce stabilization at 8 per cent moisture content and the piece was then shipped to the tropics where the average atmospheric conditions tended to produce 15 per cent moisture content, it could be expected that the part would eventually stabilize at 15 per cent moisture content, although it might take six months to a year before this value was attained. If, however, the airplane on which this piece was mounted was moved to various parts of the country at frequent intervals, the moisture content of the piece would not materially alter, since the low rate of permeability of the modern synthetic resin finishes would protect against rapid fluctuations in humidity.

Although little research has been done to date on the moisture absorption characteristics of the impregnated wood veneer materials, it is reasonable to expect that their sensitivity will be of the same order of magnitude as the thermosetting phenolic materials, since the impregnation protects the wood fibers to the same degree as the cotton fibers are protected in fabric base phenol fiber.

Relative Cost

The foregoing factors all pertain to the functional aspects of the application, but the analysis would not be complete unless the economic aspect were also considered. The thermosetting and thermoplastic materials are either molded in steel dies under relatively high pressure or manufactured as sheet, tube, and bar stock that is fabricated by shearing, punching and machining. At first glance it would seem that the attendant die expense would eliminate molded plastics from consideration in the present aircraft quantity brackets, but such is not the case because the metal parts against which they are competing are not manufactured by high production processes and also because in many cases one plastic part replaces an assembly consisting of several metal parts. Experience has indicated that molded plastics usually can compete with strong aluminum alloy castings on a cost basis if the quantities involved are above 1,000 and that where the molded plastic part replaces a complex assembly consisting of several metal parts the cost will be in favor of the plastic on quantities of 300 or more. These figures are, of course, approximate but serve to indicate the limits of the quantity brackets in which cost equality occurs.

The principal reason why molded plastics compete in aircraft in such low quantity brackets is that the molded plastic part comes out of the mold in its finished form, while the metal counterpart generally requires additional operations. The cost of fixtures and labor for performing these additional operations on the metal offsets the cost of the die for manufacturing the plastic part. The material costs are usually about the same with any small difference being in favor of the plastics. It follows that where the metal counterpart would be relatively simple to manufacture the quantities must be large before molded plastics become economical and that where the metal counterpart is relatively complex molded plastics may be economically used in quantities of only a few hundred. In quantities smaller than those discussed molded plastics will be the more expensive and should not be used except in applications requiring specific properties, such as electrical insulating value, heat insulating value, or transparency.

Although molded plastic parts are uneconomical in quantities lower than 300 to 1,000, the same is not true of items such as transparent enclosures, which are made from thermoplastic sheet stock formed to shape, since the cost of the fixtures for forming the plastic sheet is relatively small. Frequently, by designing the transparent plastic parts to be self-supporting, it is possible to eliminate a great deal of the metal framing used in transparent closure designs, and the savings made by this elimination more than offset the cost of the forming fixtures for the plastics, even when small quantities are involved.

With reference to the wood veneer materials, the unimpregnated type is competitive with metal even in small quantities, since the cost of fixtures and tools for manufacturing formed plywood shapes compares favorably with the cost of metal fixtures and tools in like quantity lots. The wood veneer materials of the unimpregnated type may therefore be used in any quantity bracket that would be contemplated for metal construction. There are not sufficient data available to draw any conclusions regarding the economic quantity brackets pertaining to the impregnated wood veneer materials, but it is reasonable to assume that the quantities will fall approximately midway between

economic quantities for molded plastics and those for unimpregnated wood veneer materials.

Limitations of Each Type of Material

The limitations of the various types of material from the physical properties standpoint have been adequately covered in the foregoing discussion of the various factors affecting design, but the engineer must also have some knowledge of their limitations from a fabricating or manufacturing standpoint, for if such knowledge is lacking there is always the danger of designing a part that is not capable of being manufactured in existing equipment.

Limitations of Thermosetting Materials

In the case of the laminated thermosetting materials, existing press equipment limits the maximum sizes of flat sheets to approximately $3^1/_2$ by $5^1/_2$ ft., although a few specific manufacturers may have presses that will handle slightly larger sizes. In the case of the formed laminated parts, such as shallow pans and door panels, the maximum sizes are somewhat smaller in order to provide adequate strength in the special dies used in manufacturing this class of product. Usually, formed laminated parts should not be larger that 3 by 4 ft. if the parts are to come within the capabilities of a majority of the manufacturers. The minimum thickness in which the materials can be fabricated depends largely on the materials used in the laminations. With the heavier weights of fabric, normally known as "C" grade materials, it is inadvisable to use thicknesses less than $^1/_{16}$ in., as thinner gauges do not have a sufficient number of laminations to provide uniformity in physical characteristics. If a part is to be made thinner than $^1/_{16}$ in., it is advisable to use light weight fabric, normally known as "L" grade, in order to avoid the afore-mentioned difficulty. It is possible to make paper base laminated materials in almost any thickness but its lower impact strength makes it inadvisable to use this grade of material in thicknesses less than $^1/_{16}$ in., as the thinner gauges are too subject to accidental damage in handling and service.

In designing parts to be made from high impact thermosetting materials, the designer should avoid the use of sections thinner than $^5/_{32}$ in. even in relatively simple parts, and this minimum should be increased for more complex parts in order to prevent distortion and provide uniformity in physical characteristics. If too thin sections are used, it is difficult to make the high impact materials flow sufficiently to fill the mold completely and, even if the mold does fill, there are apt to be large variations in the physical properties due to the resin running ahead of the filler. The lower impact materials will mold satisfactorily in thinner sections down to as low as $^5/_{64}$ in. thickness, but designers should be extremely cautious when using these materials because of their low impact strength, particularly in thin sections. In general, the low impact thermosetting materials are not advocated for aircraft use except in a few special instances, such as instrument cases and knobs, where the danger of accidental impact is slight. They should not be used in structural applications because their impact resistance is materially lowered when they are under stress.

Parts molded from either the high or low impact thermosetting materials should not generally have a projected area greater than 200 sq.in. or a maximum dimension greater than 20 in., for the presses that will accommodate sizes bigger than this are limited in number. The majority of the thermosetting molders use various sizes of press equipment up to approximately 600 tons and 2 ft. square platens, which means that in producing a part having 200 sq.in. of projected area they must use pressures less than 3 tons per sq.in. even in the 600-ton presses. Considering that it is often advisable to use pressures as high as 5 tons per sq.in., the designer can readily realize the necessity of keeping the sizes of molded plastic parts well within the limitations mentioned, particularly when it is remembered that a majority of the press equipment is of even smaller capacities. In utilizing the high impact thermosetting molding materials, it should be borne in mind that these materials do not readily flow during the molding process and that it is therefore difficult to produce a piece having deep flanges parallel to the molding direction, since such flanges must be filled entirely by flow of material. If such design details cannot be avoided, it is best to make such sections as thick as possible so that the resistance to flow will be reduced to a minimum. When inserts are incorporated in such parts they should be designed so that they can be adequately supported in the mold to avoid the danger of their being distorted or displaced by the severe flow conditions. Where severe flow conditions exist they can be materially reduced by the use of the transfer molding process rather than the compression molding process. The transfer molding process differs from the compression molding process in that the material is heated under pressure and reduced to maximum flow conditions in a separate chamber and is then squirted into the mold cavity where the cure is completed, whereas in the compression molding process the entire action takes place in the mold cavity and therefore creates far greater abrasion and flowing stresses in the cavity than exist in the transfer process.

Limitations of Thermoplastic Resin Materials

The transparent thermoplastic sheet stock is generally made in sheets 20 by 50 in., although in the case of the transparent acrylics, Plexiglas and Lucite, these sizes have been recently increased to approximately 50 by 60 in. When designing transparent parts these basic sheet sizes should be kept in mind and parts should be so designed that they can be formed from flat sheets of the foregoing sizes. If larger transparent assemblies

are to be made, it is necessary to design them so that they can be formed in several pieces and then assembled. This assembly can be accomplished either by the use of metal frames or by cementing the various formed sheets together using plastic stiffening members at the joints.

A majority of the molded thermoplastic parts is manufactured by the injection molding process, although the compression molding process is used to some degree. Injection molding machines are rated by their ounce capacity, which indicates the maximum weight of the part they are capable of handling. Only a few of the molders have machines of capacities greater than 12 oz., and a majority of the machines in operation are of still smaller capacities. Therefore, in designing parts to be injection molded from thermoplastic materials, the engineer should be careful not to exceed the capacities of the generally available equipment. The injection molding process permits the incorporation of much more delicate inserts than can possibly be molded by the compression molding process, particularly in the thermosetting materials. Compression molding of the thermoplastic materials is only used where the size of the piece is such that it cannot be handled by the injection molding machines or where the quantities involved are so small that it would not be economic to set up an injection machine for such a short run. Most injection machines operate on a 15 to 30 sec. molding cycle and, therefore, they are only used where large quantities are involved. The size limitations for compression molding of the thermoplastics are approximately the same as the size limitations for compression molding of the thermosetting materials. Parts that are designed to be molded from the thermoplastic materials should have sections at least $^1/_{16}$ in. thick, but heavier sections are advisable if the part is intricate or if it is subject to low temperature impact. The size of the parts that are to be injection molded must be designed within the space limitations of the equipment on which the molding is to take place, and it is best in such instances for the designer to contact several possible molders and determine what sizes can be handled on their equipment.

Limitations of Wood Veneer Materials

The limitations of the flat, laminated, unimpregnated wood veneer materials—in other words, normal plywood and laminated wood—are well known and need no further comment. The limitations on formed shapes of unimpregnated wood veneer materials depend on the process used and on the equipment available. Where such formed shapes are made in hot platen presses, the maximum size limitations are usually somewhat smaller than for normal plywood sheets because of the space taken up by the forming dies. Where formed shapes are made by one of the bag molding processes, the size limitations depend on the dimensions of the autoclave or boiler in which the part is cured under heat and pressure. There are several autoclaves in operation of approximately 6 to 8 ft. diameter by 20 ft. in length, and some of the companies are considering installing autoclaves as large as 10 ft. diameter by 40 ft. in length. The bag molding processes therefore place little or no limitation on the size of parts and, since the pressures used in these processes are relatively low (being approximately 40 to 100 lbs. per sq.in.), wood dies may be used in many cases, particularly where small quantities are involved, without danger of of distortion. Where large quantities and accurate interchangeability are required, it is best to use metal dies, but these are not necessarily expensive. In the bag molding process the die and the part are both in the autoclave and therefore the die is not subjected to pressure stresses such as it would be if it formed part of the surface of the pressure chamber. The die can, in most cases, be made as a skeleton die from sheet metal.

Wood veneer materials of any thickness from $^1/_{32}$ in. upward can be made by either process, but in applications in which there is danger of accidental impact, such as the exterior surfaces of an airplane, it is advisable to use material at least $^3/_{32}$ in. thick. Internal parts, such as wing rib webs, may be made from thinner materials, since they are not subject to the accidental handling loads which may be imposed on exterior surfaces. When wood veneer materials are used to form part of the exterior surfaces, it is preferable to use at least 5 plies in their construction in order to reduce the possibilities of surface checking and fracture of the protecting finish. Three-ply plywood construction always produces a sheet that is notably stiffer in one direction than the other, and this difference tends to produce cracking or checking of the surface plies under vibratory conditions, particularly where fluctuating atmospheric conditions also exist. This difference may be greatly reduced by increasing the number of plies, resulting in a sheet having a more uniform stiffness and greater ability to withstand the effects of vibration and weather. In the fabrication of such laminated wood veneer materials it is also advisable to use relatively thin face plies, as these are less subject to surface checking than thick ones.

Since the unimpregnated wood veneer materials must be adequately protected from the weather, it is essential that the finishes be so applied that all surfaces are completely covered. In order to accomplish this result it is often necessary to use a mask when finishing parts that are subsequently to be glued, and where such masks are used they should be so designed that only a minimum amount of material is left unprotected in the final assembly. This means that the mask not only must be of the same width as the glue area it protects but also the assembly must be accurately indexed so that the masked area corresponds exactly with the joint being glued. In many instances, applying the finish by dipping will avoid the use of masks, but when

this is done the designer must be careful to provide adequate access and drainage for the finish, otherwise there will either be unfinished areas or large amounts of finish trapped in the structure. In designing assemblies to be made from wood and wood veneer materials, end-grain gluing should be avoided, for such joints cannot be relied upon to carry stresses and yet are expensive to manufacture since the parts must be made to such close fits in order to accomplish this type of joint. In almost all cases this type of joint can be avoided by the use of glue blocks that can be manufactured as standard parts and readily installed. In order to avoid the possibility of the workman attempting to make such end-grain joints, it is preferable to show a $^1/_{16}$ in. clearance on the drawing so that the parts will be definitely scant and the joints made entirely of the glue blocks. Finish should be applied to such end grain prior to assembly in all cases where it is not definitely certain that complete coverage will be obtained in final finishing.

The impregnated wood veneer materials of high density, approximately 1.4, have to date only been molded by the hot press method, and therefore the sizes available in this material are limited to the platen sizes of the hot plate presses used or approximately a maximum size of 4 by 8 ft. If formed parts are made from this material, the sizes will be somewhat reduced because of the space taken up by the die. The impregnated wood veneer materials of low density (approximately 0.6) may be processed in either of two ways: (1) The veneers may be impregnated, coated with adhesive, assembled to form the laminated material, and cured and bonded in one operation in a hot press; (2) the veneers may be impregnated and cured, after which they may be assembled and bonded, using normal hot- or cold-setting resin adhesives. If the first process is used, the materials can only be advantageously manufactured by the hot press method, but, if the second process is used, the impregnated cured veneers can be handled in a bag molding process in the same fashion that the unimpregnated veneers are handled. Therefore, this process can be used to manufacture parts of either unimpregnated veneers or of low density impregnated veneers. The use of impregnated cured low density veneers in the bag molding process will eliminate many of the objections to unimpregnated wood construction from the standpoint of moisture absorption as previously discussed.

Tolerances

The laminated thermosetting materials can be machined to approximately the same tolerances that are used at present for metal parts, except that ground finishes cannot be produced to the same accuracy as is possible with metal. The tolerances needed in the manufacture of thermosetting and thermoplastic parts depend on the complexity of the part and on its physical dimensions. In general, a tolerance of ± 0.0025 in. per in. of dimension is satisfactory for dimensions that are entirely controlled by one part of the mold. If, however, a dimension is controlled by more than one part of the mold, an additional tolerance of ± 0.005 in. should be allowed for each mold joint line. If a dimension extends across a flash line—that is, between plunger and mold—an additional tolerance of ± 0.010 in. should be allowed. The above tolerances are for general molding practice and can, in special instances, be reduced to as little as 40 per cent of the above values but with attendant increase in die cost and rejection. Although shrinkage allowance is made in designing the mold, the foregoing tolerances are necessary in order to take care of variability between different batches of material and minor variations in molding temperature and pressure.

In manufacturing large parts, such as pilot's enclosures, from formed transparent sheet stock, the tolerances must be wider in order to accommodate the existing methods of manufacture. In general, a tolerance of $\pm^1/_8$ in. should be allowed on contours and on all dimensions except those that may be controlled by drill jigs or like fixtures. Closer tolerances may be used in local regions on such structures, but these generally require special machining operations after the part has been fabricated.

In parts manufactured from the wood veneer materials the normal tolerances at present used in sheet metal construction will generally apply, except that the gauge of laminated wood veneer materials may vary through wider limits than are normally present in aluminum alloy sheet stock. Formed sheets of laminated wood veneer materials up to $^3/_{16}$ in. thick can be controlled in thickness within tolerances of $\pm^1/_{64}$ in., but when the thickness approaches $^1/_2$ in. it is best to increase the tolerance to $\pm^1/_{32}$ in., although closer tolerances are possible by special selection of veneer. In designing matching parts in wood veneer construction, allowance should always be made for the thickness of the finish as well as for the general tolerances, otherwise the addition of the finish may make assembly difficult.

Applicable Specifications

The Army and Navy have issued material specifications covering laminated phenolic sheet, tube, and bar for electrical uses, molded phenolic materials for electrical uses, transparent thermoplastic sheet, and resin-bonded aircraft plywood of the unimpregnated type, but government specifications are not available for structural thermosetting molded materials, for structural thermosetting laminated materials, for structural thermoplastics, or for impregnated wood veneer materials. Government specifications have been issued to cover adhesives and finishes in certain specific classes of materials, but additional specifications are necessary before this field is adequately covered.

The most urgent need at the present time is for specifications covering the structural plastic materials,

particularly the molded thermosetting plastics. If the existing specifications on electrical materials are used for structural parts, they are unduly restrictive in that the part must then be controlled for electrical properties, which entails sacrifice in the physical properties. In order to obtain the maximum electrical properties in thermosetting plastics a high resin content is required, and this reduces the mechanical strength, particularly the tension and impact strengths, materially below the values that might otherwise be attained if the resin content were adjusted for maximum strength. The Air Corps and Bureau of Aeronautics are at present cooperating on a program to develop such structural plastic specifications, but until the new specifications are issued the aircraft manufacturers must resort to use of the specifications of the various raw material suppliers. Most of the raw material suppliers have technical data books in which they list the complete mechanical and electrical properties for each of the materials they produce. From inspection of these technical data books the aircraft manufacturer can select a group of materials which fulfills his needs. The aircraft manufacturer can then set up his own procurement specifications until such time as the Army and Navy issue specifications controlling the structural materials.

Availability

In determining the availability of plastics and allied materials from an aircraft standpoint, not only the raw materials but also press equipment and tools and dies must be considered. Since aircraft receives top priority on plastic raw materials and since the total volume produced exceeds by many times any possible aircraft demands, there should be no difficulty in readily obtaining adequate supplies of raw materials. As the plastic molders and laminators have always had sufficient press equipment to absorb the entire production of the raw material suppliers, there should be ample capacity to take care of all aircraft needs. In the molded plywood industry, particularly where the bag molding process is used, there is a possibility that a shortage of molding equipment will exist if the entire aircraft industry attempts to take full advantage of molded plywood products, but, since the equipment consists mainly of simple pressure boilers, it should not be difficult to expand the facilities in time to meet increased aircraft demands.

The development of adequate facilities for the production of tools and dies in which to manufacture the plastic parts is one of the main problems before the plastic molders and laminators at the present time. Only a few of the plastic laminators and molders are equipped to produce 100 per cent of their die requirements in their own plants, by far the greatest majority relying on outside machine shops for all or part of their die requirements. These tool and die facilities were adequate for peacetime operation when manufacturers were doing a large volume production of relatively few parts, but, when it is considered that aircraft requires a relatively small production of a large volume of parts, it can be readily realized that the demand for tools and dies will be greatly increased and that plastic manufacturers should therefore make every effort to increase their tool facilities to meet the changing conditions. If this is not done the aircraft industry will be restrained from taking full advantage of these materials.

Possible Savings of Aluminum

The extensive utilization of plastics and allied materials will also be of appreciable aid in relieving the current aluminum situation.

1. If the aircraft industry would utilize the thermosetting plastics where practicable in secondary structural and nonstructural applications, it would be possible to save up to 20,000,000 lbs. of aluminum per year without jeopardizing the safety of the airplanes by attempting to use the material in highly stressed parts or questionable applications.

2. If the aircraft industry would utilize thermoplastic materials for all transparent enclosures and for other suitable internal parts, it would be possible to use approximately 10,000,000 lbs. per year, of which up to 1,000,000 lbs. might be assumed to replace aluminum alloy support frames and miscellaneous parts.

3. If the aircraft industry would utilize the wood veneer materials for such subassemblies as bomb bay doors, wheel well doors, bulkhead doors, removable wing tips, table tops, cabinets, and miscellaneous equipment items, it would be possible to save up to 20,000,000 lbs. of aluminum per year without attempting to extend the use of the wood veneer materials to such items as fixed tail surfaces, wing outer panels, or fuselages.

This means that a total of 41,000,000 lbs. of aluminum per year could be economically saved by the use of plastics and allied materials in applications in which they are equally suitable and in which they do not affect the overall design of the airplanes upon which they are used. In other words, the utilization of plastics and allied materials would save in one year approximately six times as much aluminum as was collected throughout the entire country in the recent aluminum drive.

Review of Present-Day Welding of Aircraft Steels

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SUMMARY

The purpose of this paper is to give a review of present-day welding in aircraft steel tubing construction. The various types of welding are discussed, together with a brief fundamental treatment of the welding process and its association with the hardenability and hardness of X4130 steel. The merits of preheating and postheat treatment are discussed.

The second part of the paper deals with some considerations of the physical characteristics of steel tubing specially prepared with high proportional limits. The effect of welding on this material is also explained.

INTRODUCTION

THE INTENT OF THIS PAPER is not to present any expert opinions on aircraft welding. However, it is thought that a general recheck of the whole subject of welding in aircraft would be of some interest at this time. At the end of the paper some thoughts are given for some future research in the hopes that such research will point the way to increased safety and a higher strength-weight ratio.

X4130 CHROMIUM MOLYBDENUM STEEL

The majority of steel used for welding in aircraft construction is the high strength, low alloy type. The steel that is the most important and most commonly used is S.A.E. X4130. It is used most often in the form of tubing. The tubing is welded into various types of structures, such as engine mounts and fuselages, and is used in many other places where the combination of high strength and light weight is essential. Many of the landing gear cylinders and piston tubes are made from X4130, although a lot of the larger assemblies are made from X4135 and X4140. Since most of the steels that are welded are somewhat similar, the welding phenomena are essentially the same.

Inasmuch as we are dealing with a steel that has high hardenability as one of its most important assets, a brief discussion of this term is in order. Hardenability is a function of the chemical composition and the mass of the steel in question. The hardenable elements in X4130 are carbon, chromium, manganese, and molybdenum. When steel that contains appreciable amounts of these elements is heated to above the critical temperatures and rapidly cooled, the steel becomes hardened. The element carbon is undoubtedly the greatest contributor to hardness in steel.

X4130 has exceptional hardenability considering the low percentage of alloying elements. Even in heavier sections this hardenability is quite marked. Inasmuch as the tubing used in aircraft construction is of light gauge, the hardenability is even more pronounced.

Because of the exceptional hardenability of this steel, some troubles in welding are often encountered when the proper technique is not observed. The majority of welders do not have any trouble when welding steels under 0.25 per cent carbon. However, when the carbon is increased to as high as 0.33 per cent, and especially when very light sections are used, the troubles in welding are seriously increased. The effects of various elements on weld hardness are noted in Fig. 1. It is interesting to see from this photograph that carbon is without a doubt the most effective element in producing hardness.

When a steel of this particular composition (X4130) is heated to above the critical temperature (1,500°F.) and quenched into water, an exceptionally high hardness is obtained. This hardness may be even higher than 500 Brinell (Rockwell C-50) corresponding to a tensile strength of 275,000 lbs. per sq.in.

WELDING

In the welding process the parts to be welded are heated considerably above the critical temperatures, and immediately upon removal of the welding heat, the surrounding metal, which is cold, acts as a rapid conductor from the welded area. By this means there is obtained a high temperature gradient that may be even greater than that obtained with quenching in water. It is obvious that one would not think of using material, such as X4130, which has been quenched rapidly in water without applying some form of tempering or drawing so as to reduce the hardness and make the piece of steel usable. However, it is common practice to leave the majority of the welded structures in aircraft in the "as welded" condition where the hardness of the joint may be in excess of 500 Brinell, Rockwell C-50.

The exceptional hardness developed in an arc-welded joint is noted in Fig. 2. The hardness readings are taken on the tube wall—in this particular case a crack developed at the root of the fillet. This crack took

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* Chief Metallurgist.

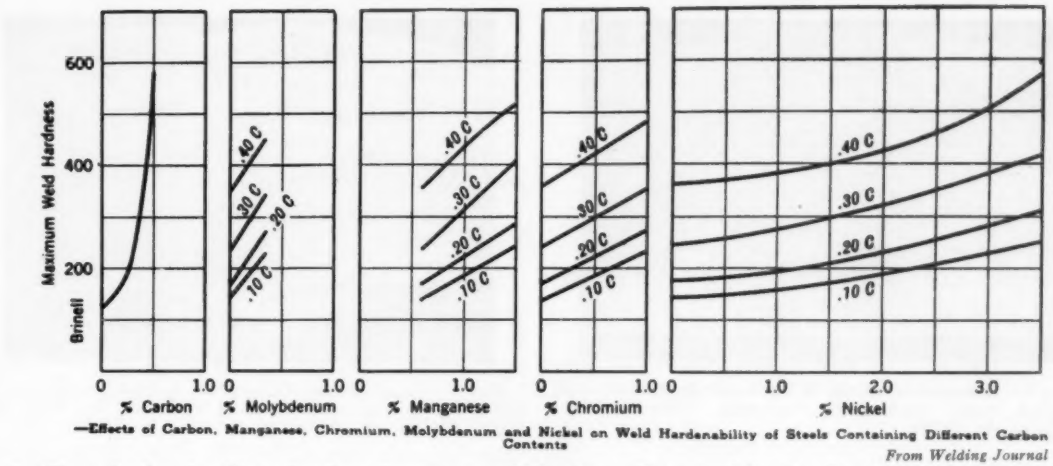

FIG. 1. Chart showing the effects of various elements on weld hardness. Note especially the effect of carbon when present in amounts over 0.25 to 0.30%.

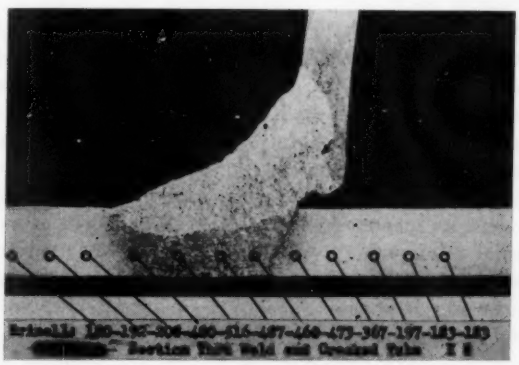

FIG. 2. Enlarged reproduction of section through weld area of an arc weld on X4130 steels. Note hardness variations and especially the 516 Brinell in weld. The crack is through tube wall at root of fillet.

place not at the point of the highest hardness but at the root of the fillet where stress concentration was greatest.

The internal structure of the metal which is quenched in water and internal structure of the hard zone in the welded joint are similar. These hard structures developed in quenching are known as martensite, and one can fully appreciate the words of an eminent metallurgist who said: "Weldability is the antithesis of hardenability and the bane of a welder's existence is martensite."

So with this dark picture we enter into a review of present-day welding practices and a start of what is going to be done in the future to make welded steel structures stronger and safer.

GAS WELDING

Up until two or three years ago most of the welding on aircraft tubing was done with the oxyacetylene flame; although this method is still used to a large extent and will continue to be, it is being rapidly replaced by arc welding. The oxyacetylene flame is particularly adapted to thin gauges, whereas the arc welding is generally limited to gauges 0.050 in. or thicker. In gas welding there is necessarily a large amount of heat applied to the joint to be welded, and with this large input of heat there is the associated expansion and subsequent contraction resulting in considerable distortion. However, a lot of the distortion can be eliminated by proper jig fixtures and method of welding.

It is important in oxyacetylene welding to have a good control of the flame, and every attempt should be made to keep the flame as nearly neutral as possible. However, because of the difficulty of maintaining a neutral flame, it is generally maintained at a harmless, slightly reduced condition. The amount of the heat or the size of the flame should be controlled so that it is just sufficient to cause the weld to be made and to keep at a minimum the heat input at the weld. The general nature of oxyacetylene welding is such that the heat of the flame imparts some residual heat in the metal surrounding the welded joint, thus minimizing the exceptionally hard region of the weld area. The details of this residual heat or preheat will be discussed later in the paper.

ARC WELDING

In the last year or two arc welding has made great strides in replacing gas welding because of the improvement in arc welding equipment. The manufacturers are to be complimented on making equipment that will handle light gauges used in aircraft tubing and sheet. The particular advantages of the new equipment are the low amperage obtainable and, to a certain extent, crater elimination.

Arc welding is particularly rapid and so much faster than gas welding that speed is probably its greatest asset. The replacement of gas welding by arc welding on certain assemblies such as engine mounts has in-

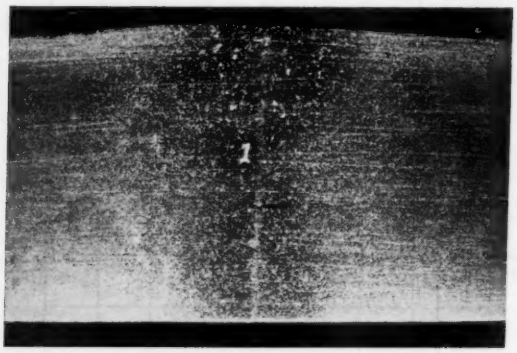

FIG. 3. Enlarged view of a resistance-flash weld with the "flash metal" removed by machining. Note the weld joint is barely visible.

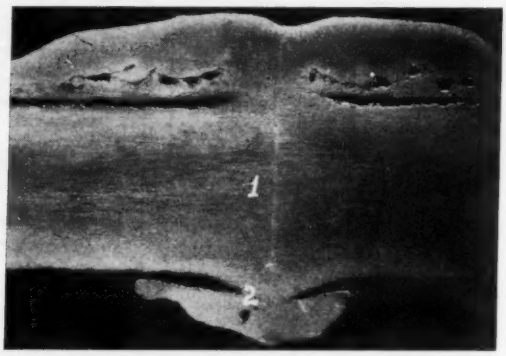

FIG. 4. Reproduction of same weld as shown in Fig. 3 showing a cross-section of the tube at weld joint but with "flash metal" not removed.

creased the production three to fourfold. In general, the engine mount that is arc welded must necessarily use heavier gauge tubing than that used in gas welding and, because of this, the weight of the engine mount has increased slightly. This increase, however, is more than offset by the faster production attained.

The nature of arc welding is such that the heat necessary to form the weld is obtained from an arc that is struck between the electrode or welding rod and the part to be welded. Obviously, this point of welding covers a very small area and, because of the minimum amount of heat imparted to the general area, there is considerably less expansion and contraction and, hence, less distortion of the whole assembly. There is, too, a more rapid conduction of heat away from the weld, thereby leaving a harder zone in the welded region than that existing in gas welding.

One large aircraft manufacturer has used arc welding for a great number of years on practically all engine mounts and tubular structures with no known failure in service.

FLASH WELDING

Within the past year considerable advances have been made in the flash or butt welding of tubes together by electrical resistance. This, of course, is done by holding the two parts to be joined in clamping devices. The two parts are then brought slowly together, striking an electric arc. When the electric arc has been on sufficiently to cause a slight melting of the contact surfaces, the two parts are forced together under pressure and, with the current cut off, the weld is complete. The pressure on the soft or molten metal causes some excess to squeeze out, thereby creating what is known as the flash. This method of welding is particularly adaptable where it is desired to weld a slightly heavier tube section on to a lighter tube so that a thread can be cut on the heavier section without decreasing the total strength. It may be necessary to specially prepare the contact surface of the heavier wall section so that like areas are brought together.

The physical properties and associated internal structures of the metal with this type of welding are particularly good. Most of the oxide that may have formed in the welding is squeezed out into the flash, thereby leaving an exceptionally clean weld. This flash weld is composed of all base metal rather than having the addition of deposited metal. Fig. 3 shows a flash weld indicating the metal of the weld. Fig. 4 shows another flash weld, showing the amount of flash.

There is also a similar method of butt welding which is done by means of oxyacetylene gas and which is referred to as pressure welding. It is believed that this method has not as yet been applied on any aircraft parts, but it should certainly be explored very soon. The parts to be welded are heated up with jets of oxyacetylene flame almost to the melting temperature and then one part is pushed against the other, thereby causing a fusion. It is reported that the flash in this particular type of welding is smooth and relatively easy to remove as compared to the flash on the resistance flash butt welding. Some research along this line is certainly in order.

SPOT WELDING

There has been considerable work done on spot welding of sheet metals made of aluminum alloys and stainless steel and a little work on X4130. Most everyone is familiar with the spot or shot weld and, because of the instantaneous heat developed by the arcing across the sheets and melting occurring at a spot and then rapidly cooling, the immediate vicinity of the spot weld is exceptionally hard in the case of X4130 material. Recently, there has been designed a method of annealing of the spot weld to eliminate the hardened zone. This annealing is done with the welding machine after the spot has been made and is controlled by a cycle timing device. There has been very little work done on the spot welding of X4130 material, although there are several places where it is being investigated, and it is felt that once the hardened zone and associated hazards

have been eliminated more spot welding will be done on tubing and sheet made from X4130.

Atomic Hydrogen Welding

There is little atomic hydrogen welding done on aircraft structures in this country, although some has been done in Europe on special types of joints.

Preheating and Postheating

In the brief description of welding mentioned previously in the paper, it can readily be seen that the hard zone adjacent to the weld exists to varying degrees, depending on the method of welding. Generally speaking, the hardened zone is less hard in gas welding than in other methods, for reasons that were previously described. Although gas welding is generally familiar and common to all plants, the techniques used in the different plants vary as much as the number of places where this method of welding is used. The jig fixtures also vary—some are more rigid than others. In plants where the welder has good jigs, and has heated the joint to be welded before attempting to weld, and has welded in the right direction, there has never been any major trouble from so-called "weld cracks." One of the biggest reasons for this is that in the properly gas-welded joint the heat input of the flame retains sufficient heat in the joint to cause slower cooling, thereby minimizing the hard zone.

In arc welding, preheating has not been generally used and the result is a high degree of hardness in the weld area. Some reduction of hardness is accomplished by depositing one bead near another. The heat of the second bead tempers the hard zone of the first, etc. In recent months, however, the majority of the aircraft manufacturers using arc welding are developing some form of preheat or postheat. There are still many who do not use either. This practice is being resisted because it tends to interfere with high production. One manufacturer is using a hydrogen flame for preheating before arc welding. The hydrogen flame prevents any oxidation and its heat also decreases the hardness at the joint. Many manufacturers are strain-relieving or postheating welded assemblies after arc welding. The effects of preheating are shown in Fig. 5.

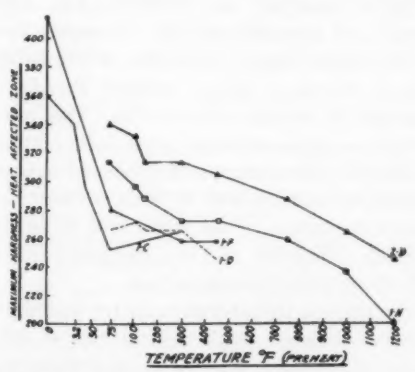

Fig. 5. Chart showing the effect of preheating temperature on hardness of weld area for various steel analyses.

There are several large airplane manufacturers who are normalizing engine mounts and other structures after welding, and in spite of the warping, distortion, etc., which may happen, this distortion is at a surprising minimum with proper jig and cooling devices. Any straightening necessary is better done on a normalized structure than one in the "as welded" condition.

There is no doubt in the mind of those having a fair metallurgic knowledge that some form of preheat, postheat, or other heat treatment to alleviate the hardness present at the weld is just common sense.

Steel Melting Practice

In order to supply the aircraft industry with steel tubing that would be as free from susceptibility of the human element as possible, aircraft tubing producers and steel makers have joined in a combined effort to make the steel as foolproof as possible. The elements tending toward increased hardenability of the steel are controlled and the carbon content is limited to 0.33 per cent.

In addition to the control of the hardenable elements of steel, manufacturers all began to use electric furnace steel in which the sulfur and the phosphorus content were held to absolute minimums. As compared to open hearth, electric furnace steels of the same composition are generally better deoxidized and controlled. To further decrease the hardenability factors, fine grain steel has been supplied for nearly three years and grain size control is now a part of all government specifications for aircraft tubing.

The Future of Welding of Aircraft Steels

There are no data readily available on the fatigue-impact values on welded aircraft tubing joints. It is believed that the future of aircraft welding will best be predicted when the fatigue and fatigue-impact properties of welded joints are known. Such a program of study is now under way at Battelle Memorial Institute on arc-welded assemblies. It is believed that the results of fatigue and fatigue-impact tests on welded joints will bring out the benefits of either preheat or postheat as compared to an "as welded" joint. The results of the investigation at Battelle Memorial Institute will probably be finished within a year, and it is hoped that the aircraft companies will take special account of what is accomplished as a result of this investigation.

Some Interesting Observations in Welding of High Stress Elastic Materials

For a long time the British have been using X4130 tubing, which has been processed by normalizing, cold drawing, and strain relieving in this order. Such tub-

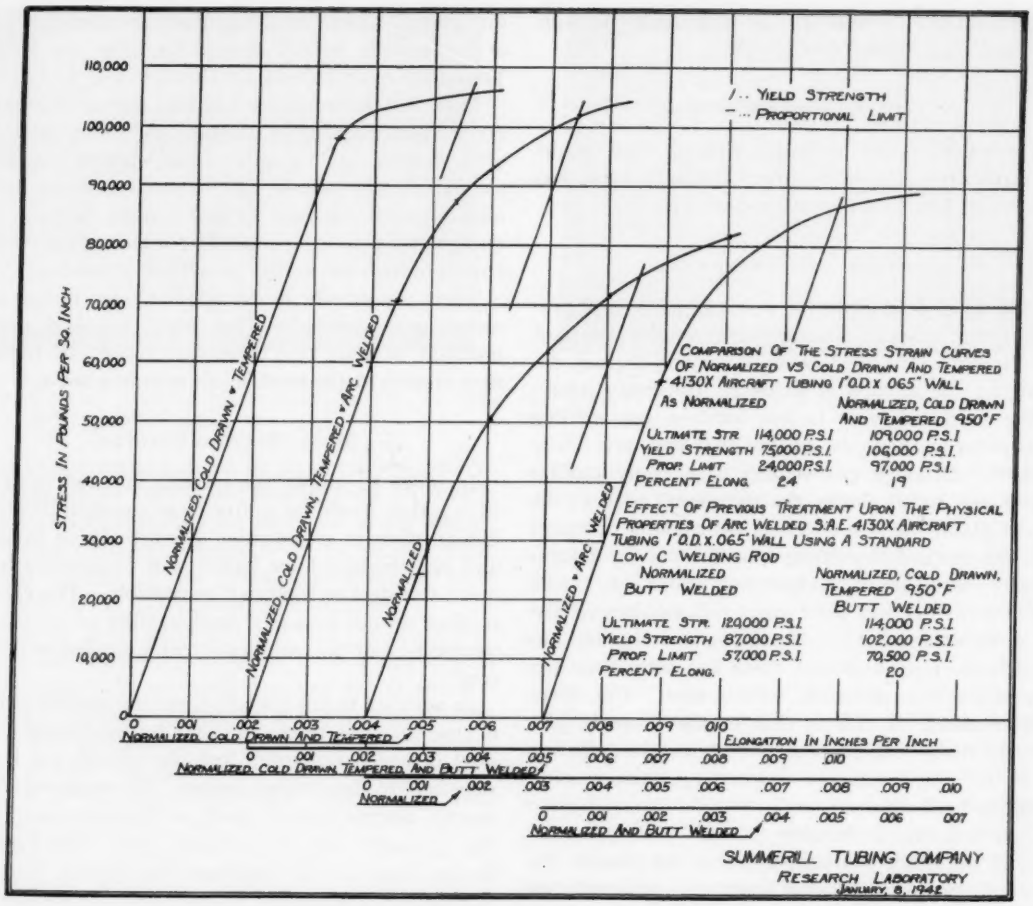
STRESS IN POUNDS PER SQ. INCH
110,000
100,000
90,000
80,000
70,000
60,000
50,000
40,000
30,000
20,000
10,000
0
... YIELD STRENGTH
- ... PROPORTIONAL LIMIT
NORMALIZED, COLD DRAWN + TEMPERED
NORMALIZED, COLD DRAWN, TEMPERED + ARC WELDED
NORMALIZED
NORMALIZED + ARC WELDED
COMPARISON OF THE STRESS STRAIN CURVES OF NORMALIZED VS COLD DRAWN AND TEMPERED 4130X AIRCRAFT TUBING 1" O.D. X .065" WALL
AS NORMALIZED
NORMALIZED, COLD DRAWN AND TEMPERED 950°F
ULTIMATE STR. 114,000 P.S.I. 109,000 P.S.I.
YIELD STRENGTH 75,000 P.S.I. 106,000 P.S.I.
PROP. LIMIT 24,000 P.S.I. 97,000 P.S.I.
PERCENT ELONG. 24 19
EFFECT OF PREVIOUS TREATMENT UPON THE PHYSICAL PROPERTIES OF ARC WELDED S.A.E. 4130X AIRCRAFT TUBING 1" O.D. X .065" WALL USING A STANDARD LOW C WELDING ROD
NORMALIZED BUTT WELDED
NORMALIZED, COLD DRAWN, TEMPERED 950°F BUTT WELDED
ULTIMATE STR. 120,000 P.S.I. 114,000 P.S.I.
YIELD STRENGTH 87,000 P.S.I. 102,000 P.S.I.
PROP. LIMIT 57,000 P.S.I. 70,500 P.S.I.
PERCENT ELONG. 20
0 .001 .002 .003 .004 .005 .006 .007 .008 .009 .010
NORMALIZED, COLD DRAWN AND TEMPERED
ELONGATION IN INCHES PER INCH
0 .001 .002 .003 .004 .005 .006 .007 .008 .009 .010
NORMALIZED, COLD DRAWN, TEMPERED, AND BUTT WELDED
0 .001 .002 .003 .004 .005 .006 .007 .008 .009 .010
NORMALIZED
0 .001 .002 .003 .004 .005 .006 .007
NORMALIZED AND BUTT WELDED
SUMMERILL TUBING COMPANY
RESEARCH LABORATORY
JANUARY, 8, 1942

FIG. 6. Chart showing a comparison of the stress-strain curves for tubing processed by different methods.

ing gives an 0.2 per cent yield stress of 89,000 lbs. per sq. in. minimum. This material is quite elastic, having a proportional limit nearly the same figure as the proof stress. Tubing that has been finished in a normalized condition has a proof stress or a yield strength of 75,000 lbs. per sq.in. minimum and a proportional limit of possibly 30,000 to 40,000 lbs. per sq.in. One will note the comparison of the two curves on the accompanying graph—Fig. 6. The normalized cold-drawn and tempered tubing has practiçally all the advantages of the "as normalized" material plus the particular properties that in the opinion of good designers are certainly advantageous. Values for arc-welded tubing in both conditions are also shown.

If the matter of the fatigue testing of a welded joint made first with normalized tubing and then with material made according to British standards is analyzed, some interesting observations will be made. Because of the high elastic properties of the British material, the stresses that are put into the specimen will not cause any plastic flow in the highly elastic material, whereas the same stresses do cause a plastic flow in the "as normalized" material. In other words, the stresses that cause plastic flow are considerably less in the normalized material than they are in the highly elastic material. The normalized material will absorb the stress and become so highly stressed itself that fatigue failure occurs at a much lower load. The highly elastic material will not absorb the stress until sufficient loading has been applied to exceed the proportional limit.

Fig. 6 shows some of the stress-strain curves on material that is unwelded and welded. The results are interesting and certainly worthy of further investigation. The higher elastic properties of the British material allow the use of higher stresses, thereby cutting down weight in aircraft construction. One interesting fact is that a section of tubing made from material that is normalized, cold drawn, and tempered is capable of being flattened almost wall to wall without cracking. This particular phase of the paper will probably cause considerable discussion, and it is suggested that further research along these lines is needed.

It is the hope of the aircraft industry and the aircraft tubing manufacturers, with the interests of safety and weight-saving in mind, that the whole welding procedure in aircraft will be standardized through research.